A MECHANICAL RECTIFIER FOR AIRCRAFT

BY

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for
AIRCRAFT

by

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PREFACE

This investigation was made at the US Naval Postgraduate School, Annapolis, Maryland, during the Academic Year 1949-50. Its successful completion is to a large extent the result of the help and information of Mr. Otto Jensen and Mr. John Kuzmack at the ITE Circuit Breaker Co. The information on the Orthonol Cores was obtained through the excellent cooperation and help of the Magnetic Amplifier Subsection of the US Naval Ordnance Laboratory; who, by permitting the author to use their equipment, shop spaces, and an Orthonol core, made it possible to obtain the characteristics upon which the calculations were based. Mr. McKee of this section was particularly helpful during this portion of the investigation.

Professor Allen E. Vivell, in his capacity as faculty adviser, gave unsparingly of his time and helped solve many thorny problems which arose from time to time during the investigation.

The author is also greatly indebted to ITE for the use of applicable circuit diagrams and wave forms from their two papers on the mechanical rectifier. And, to the Eclipse-Pioneer Division of Bendix Aviation Corporation for technical data on their 90 KVA alternator.

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SYMBOLS AND ABBREVIATIONS

ITE	ITE Circuit Breaker Co., Philadelphia, Penna.
NOL	Magnetic Amplifier Subsection, US Naval Ordnance Laboratory
i	Instantaneous current
K	Contacts
i_s	Short Circuit current during commutation
ΔT	Step Length in milliseconds
A_{Fe}	Iron core area
E	RMS Voltage
N	Number of turns of the main winding
$\Delta\beta$	Change in flux (Intrinsic) in the core during the step in webers
α	Delay in commutating time
e_c	Instantaneous commutating voltage
L_s	Total effective inductance in the commutating circuit
d_m	Mean Diameter of the core
W	Width of the core
a	Area of the hysteresis loop
z	Per unit change in pre-excitation current
I_p	AC Pre-excitation current
N_p	Number of turns of AC pre-excitation on main core
AT	Ampere turns

SUMMARY

This is an attempt to design conservatively a mechanical rectifier with a rating of 9.6 KW at 115 volts DC for large aircraft, using for its AC supply, the Eclipse 400 cycle, 90 KVA alternator.

This design gave a weight of 3.2 pounds per kilowatt and an efficiency ranging from 82.7% at 1/4 load to 92.1% at rated load. The entire unit could be put into a space 9 inches x 9 inches x 15 inches.

This type of rectifier has as its primary advantages, simple voltage control, low weight, high conversion efficiency, no altitude limitation, and no reasonable temperature limitations. (100°C ambient air)

Its chief disadvantages are, its requirement of a large capacity power source to give a reasonable power output without resort to transformers; a power factor of .85 lagging, at rated load; and a mechanical sensitivity of the contact mechanism. The extent of this mechanical sensitivity will not be known until a unit has been built and tested.

This unit was designed to have the mechanical portion driven by the same gear train which drives the AC power source. This would appear to be almost obligatory for aircraft applications, to give greater reliability and to reduce weight.

The mechanical rectifier is a good solution for the rectification problem for large aircraft and an excellent solution for large aircraft flying at high speeds at high

altitudes or where high ambient air temperatures have become a problem.

CHAPTER I

INTRODUCTION

1. General

This was a preliminary investigation to determine the advisability of utilizing the mechanical rectifier for the conversion of 400 cycle AC to DC in aircraft. As an aid to the investigation, it was decided to design a rectifier having a nominal rating of 9.6KW at 115 volts DC with provision for voltage regulation and able to deliver a peak overload of 500% rated current. No previous work had been done at 400 cycle to the knowledge of the author. In fact, only a limited amount of work has been done in the conventional power frequencies. This had been primarily on large fixed installations. The original work was done by Siemens-Schuckert, Berlin, Germany, before and during War II. It has been carried on since the War, by the ITE Circuit Breaker Co. of Philadelphia. Jensen (1), Rolf (3).

This investigation was only a preliminary one, since no information was available in the 400 cycle range and any complete analysis would have involved actually building and testing a complete installation. This was obviously impossible within the limitation of time and funds available for the project. The results obtained, therefore, are based on the tests of one Orthonol Core supplied by the Magnetic Amplifier Subsection of the US Naval Ordnance Laboratory, hereafter referred to as NOL; and the basic

relations developed by Floris Koppelman (2) and Dr. Erich Rolf, both of Siemens. Dr. Rolf has since prepared a rather complete study of mechanical rectifier design at conventional power frequencies for ITE. Rolf (3).

This investigation was undertaken because information based on work in the conventional power frequencies indicated that it was possible to achieve high conversion efficiency and good voltage control with a relatively simple mechanism, which combined these advantages with low weight and space requirements.

2. The Basic Theory of Operation.

The basic theory of operation of the Mechanical Rectifier is not very well known since the original concept was not developed until 1935 and was not known in this country until 1946. The mechanical rectifier operates by connecting the AC bus to the correct polarity DC bus during the time interval during which the AC bus can deliver energy. This connection of course must be terminated before the voltage of the AC bus falls sufficiently so that the current will reverse. Also, in any multi-phase system, there must be some provision for shifting the load from phase to phase or commutating as the process is now familiarly called.

In a conventional rectifier, such as a selenium oxide, this is taken care of by the nature of the conducting medium. In the mechanical rectifier, however, the current can flow in either direction with equal ease and some provision must be made to time properly the making and breaking of the

Considering now an elementary circuit to explain the basic theory upon which the unit depends for its operation; refer to Fig. 1, and make the following assumptions:

A. General Conditions

1. L_1 and L_2 are very much less than L_L

2. K_2 closes automatically when E_2 equals E_1.

B. For the initial conditions

1. E_2 is less than E_1

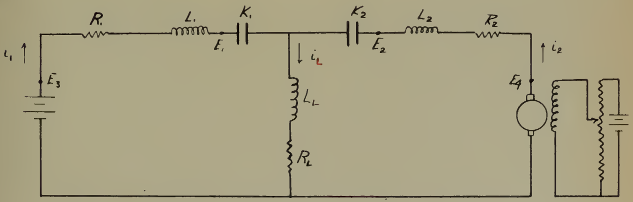
2. As a result, K_1 is closed and K_2 open.

3. i_1 has reached a steady state condition

C. Dynamic conditions

1. i_L will remain constant if E_4 is increased very rapidly to a value such that $E_4 - i_L Z_2$ equals E_1

If all of these assumptions are considered, i_1 will have decreased to zero and i_2 will equal i_L. Thus, the commutation from supply 1 to supply 2 will have been completed.



Elementary circuit

Fig. 1

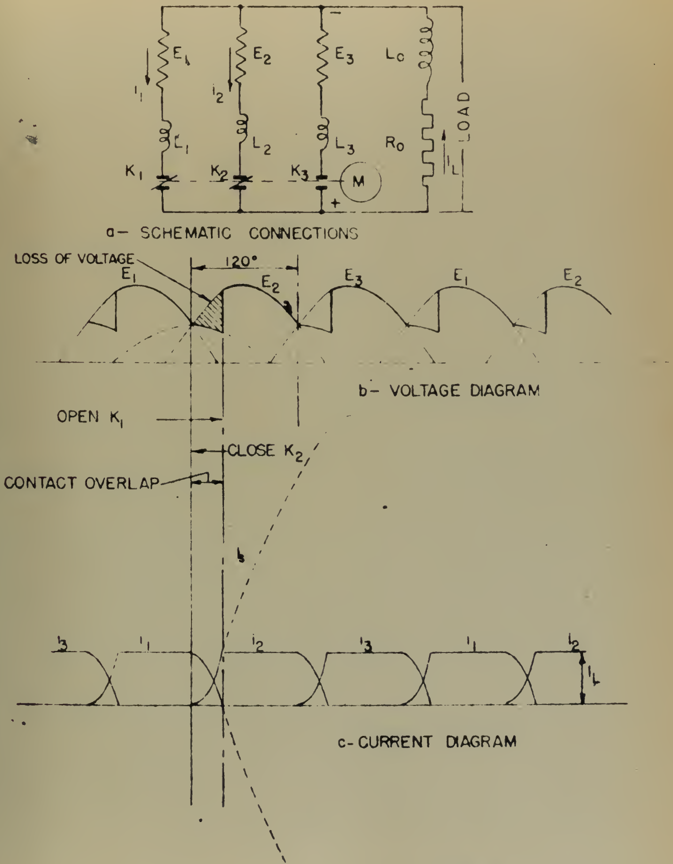


FIG 2
COMMUTATION OF CONTACT CONVERTER

If we now substitute for the voltage sources used in Fig. 1, the alternator shown in Fig. 2, we can use conventional AC transient analysis. If we again assume that L_1 and L_2 are small as compared with L_L, the following analysis will apply. Considering the closing time of K_2 in Fig. 2, the current i_2 will rise in accordance with the normal RL circuit transient response as shown in Fig. 3. Since the portion in which we are interested is only a fraction of a cycle and the resistance is necessarily small, the time constant can be assumed to be infinite and i_2 can be represented by a cosine wave displaced so that it starts from zero.

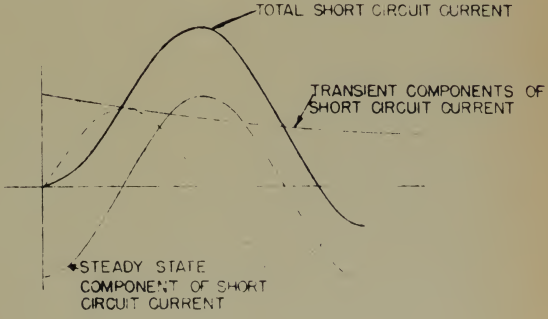


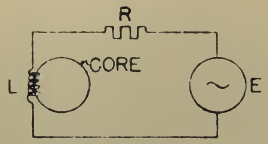
FIG 3

CURRENT & VOLTAGE CONDITIONS WHEN ENERGIZING A REACTOR AT ZERO VOLTAGE POINT

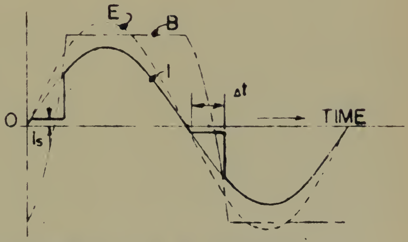
The current in the load circuit can be assumed to remain constant as a result of the large value of L_L and as a result the current i_1 will decay in the form $1 - i_{s_{max}} \cos wt$. $i_{s_{max}}$ is the maximum short circuit current which could flow. At the time that i_s equals i_L, i_1 will have decayed to zero and the commutation will be completed. At this point, of course, it is necessary for the switch K_1 to open to prevent reversal of i_1, furthermore, it must open at exactly this moment to insure arc free operation. This point, however, is a function of load current, AC input voltage, and frequency; as a result, it will shift as these variables are changed. For a three phase, star pointed connection, the wave forms would look like Fig. 2. The shaded voltage area is the change in flux linkages required for commutation. This results in a reduction in DC output voltage. It will be referred to in this paper as reactive voltage drop.

3. The Step

The physical impossibility of making the contacts always open at zero current is the problem which plagued all efforts to design a practical mechanical rectifier until Floris Koppelman reasoned that if the current could be made to remain at zero for a finite length of time, it would be possible to design a mechanical unit which would open and close the contacts during this period. The device which accomplishes this, and as a result is the heart of the unit is the saturable reactor. If a saturable reactor is placed in the circuit through which i_1 is flowing; as current reverses, the entire voltage will appear across



a- SCHEMATIC DIAGRAM



b- CURRENT, FLUX & VOLTAGE CONDITIONS

FIG 4
ENERGIZING A SATURABLE REACTOR FROM A A.C. SOURCE (RESISTIVE CIRCUIT)

the turns on the saturable core during the remagnetization time. The resulting current step, ΔT, will have a length which is a function of core material, the area of the core, A_{Fe}, the voltage across it, E, and the number of turns, N. $T = \frac{\Delta \beta A_{Fe} N}{E}$ Jensen (1). It must be continually kept in mind that β is the intrinsic magnetization, not the total amount of flux in the core. $B = \beta + \mu H$. There will still be a small amount of current flowing which is providing the ampere turns required to remagnetize the core. This is small however when compared with the load current. The effect of a saturable core in a simple circuit is shown in Fig. 4.

4. The Step Length and the Safety Step.

Referring again to the shift in the zero current point as a function, primarily, of load current and the inductance of the AC circuit, the current step must be made long enough so that it will include the time that the contact opens, regardless of what the electrical conditions may be. If this were not true, there would be an arc and the contacts would be destroyed. The two extremes are normally the minimum and maximum load as shown in Fig. 5. In most rectifiers, provision is made for shifting the timing of the contacts to compensate some for this, and as a result, reduce the size of the core required. This is done on the premise that the load current will not change any faster than the contact time can be shifted. This does not appear to be a reasonable assumption for aircraft applications and so the step was necessarily designed for fixed contact timing.

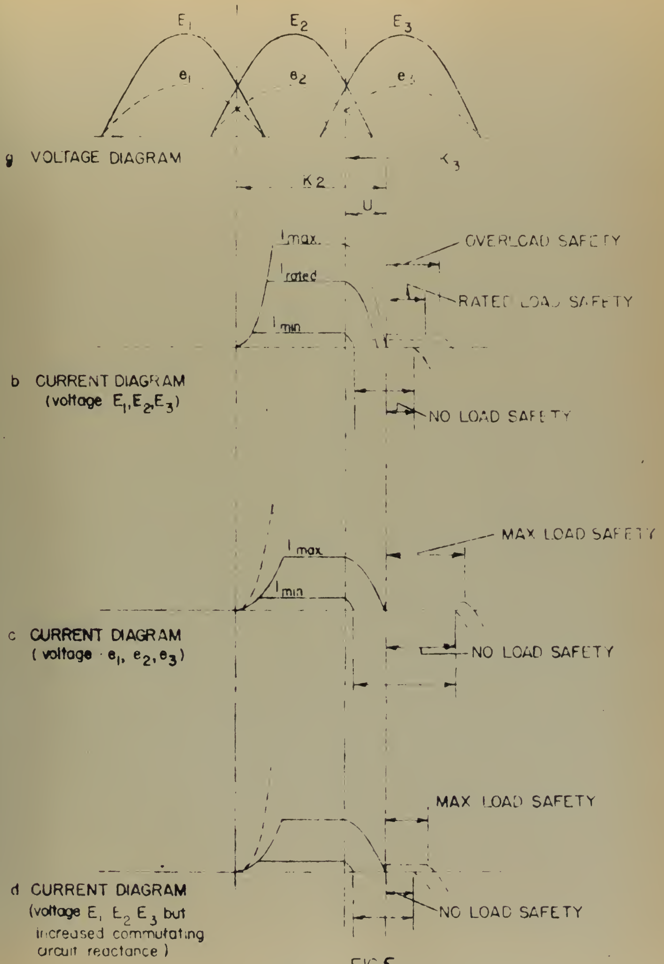


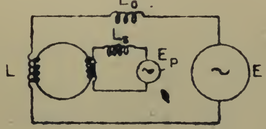
FIG 5
LOAD CURRENT VARIATION

Nobs 28577-A.

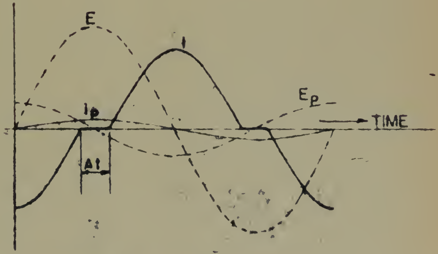
In addition to the actual step length required by the inital conditions, it is also necessary to have an additional time designated as the safety step. This is the portion of the step which extends after the contact opens as further insurance that the contacts will be far enough apart, when the voltage across the reactor collapses and appears across the contacts, that there will be no possibility of flash over. Koppelman (2) states that if the contacts open at a speed of 1 meter per second, a safety step of 10^{-5} seconds is sufficient. This is always made longer though to take care of manufacturing and installation deficiencies.

5. The Need for Pre-excitation.

The small amount of magnetizing current flowing may still produce some arcing, this would not be a serious problem if the operation did not repeat so frequently. The magnetizing current can be controled by providing a pre-excitation winding which will provide the necessary number of ampere turns for the remagnetization. This can be seen by referring to Fig. 6 which shows the results if AC pre-excitation of the proper phase and power is provided. In practice, sufficient pre-excitation is used so that the current in the main winding does not reverse but continues to flow with a small positive value during the step. This is done so that when the contacts open, a small positive voltage will exist across the contacts during the safety step, from the inductance in the circuit, before the high negative voltage which is across the core appears across



a SCHEMATIC DIAGRAM



b CURRENT VOLTAGE CONDITIONS

FIG. 6

ENERGIZING A SATURABLE REACTOR FROM AN A.C. SOURCE (INDUCTIVE CIRCUIT AND WITH A.C. PRE-EXCITATION.)

Nobs-28577-A9

the contacts. This change in polarity is merely another safety precaution to insure that any arc which may start from metal particles in the space between the contacts will go out as the current goes through zero.

6. Voltage Control

We now have a unit, Fig. 7, which will deliver a DC voltage that is a function of the AC input voltage and the inherent voltage regulation of the unit. If the time of contact closing were delayed an angle α from the closing time of K_2 in Fig. 2, to a later time, it would result in a lower DC output voltage. This can be best understood by referring to Fig. 8. This system is used in installations with provisions for shifting the contact time.

There is another system of voltage control called magnetic delay which requires no contact shift. This makes the contact mechanism much simplier to build and operate. The normal disadvantage is that it requires a large break reactor. This is not a disadvantage in this design since a large one is required for the severe overload requirements. Referring now to Fig. 9b, for maximum DC output voltage, the commutating reactor provides no delay in the build up of the phase current. However, as in Fig. 10, when some voltage control is desired, the amount that the core has been premagnetized, will determine the reduction in DC output voltage the same way as in Fig. 8, for the mechanical system. Area A_1 is the reduction in DC voltage from magnetic delay and area A_2 from the reactive voltage drop.

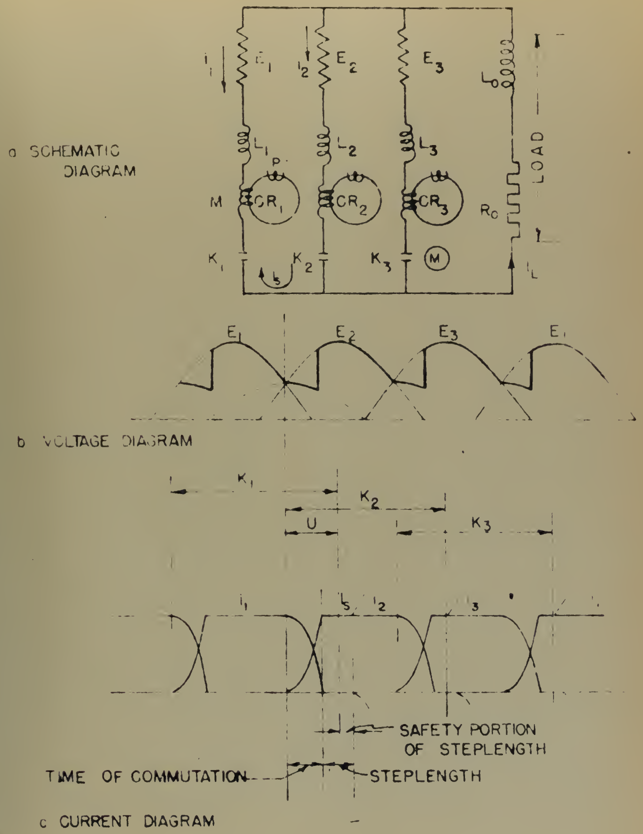
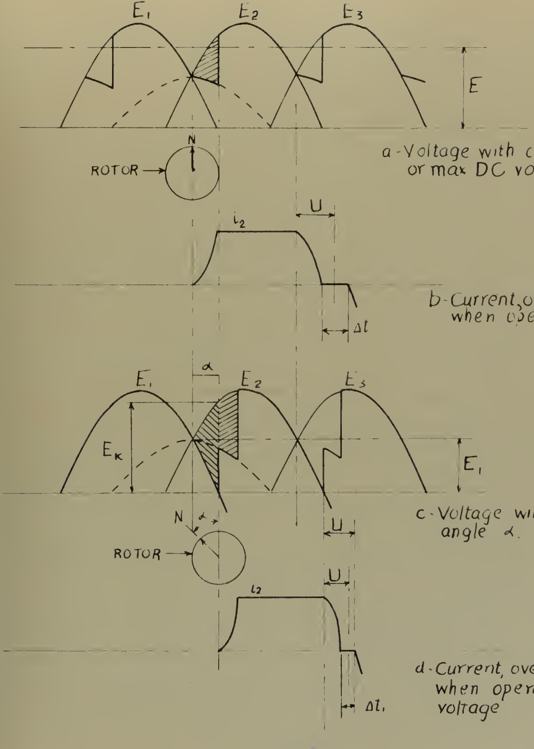


FIG 7
COMMUTATION OF CONTACT CONVERTER WITH SATURABLE REACTORS (COMMUTATING REACTORS)



a - Voltage with control angle α = 0 or max DC voltage obtainable

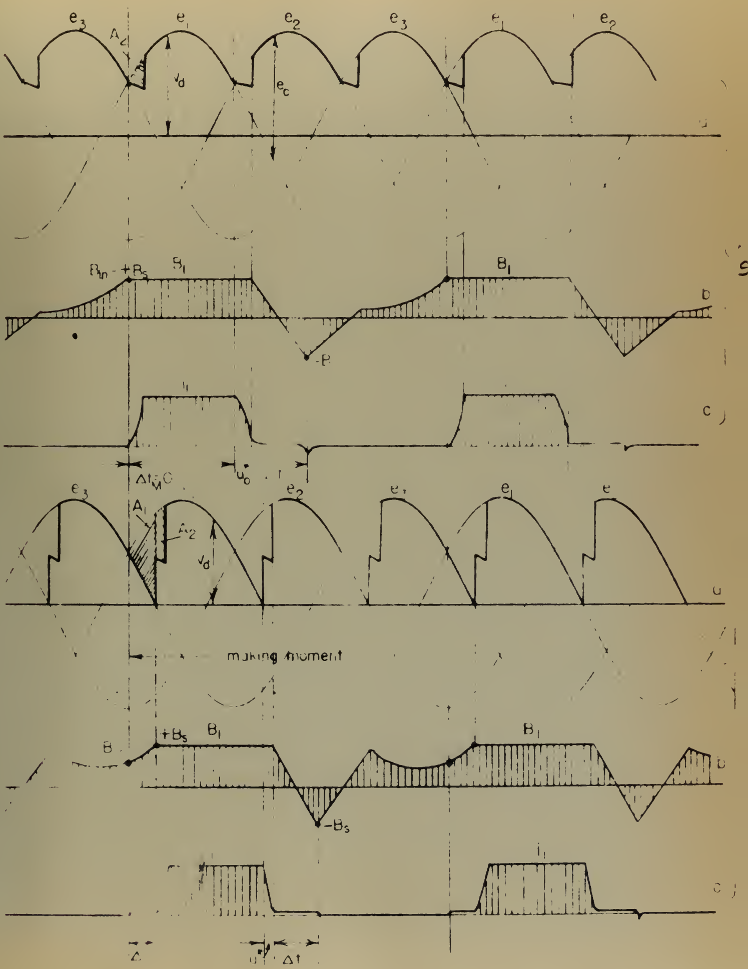
b - Current, overlap and steplength when operating at max. voltage

c - Voltage with delayed control angle α.

d - Current, overlap and steplength when operating with depressed voltage

FIG - 8

VOLTAGE CONTROL


e3
e1
e2
e3
e1
e2
A_2
v_d
e_c
a
$B_{in} = +B_s$
B_1
B_1
b
$-B$
i_1
c
$\Delta t_M = 0$
u_0
e3
A1
A2
e2
e1
e1
e
v_d
a
making moment
$+B_s$
B_1
B_1
B
b
$-B_s$
i_1
c
Δt

7. The Selection of Core Material

The step length as was pointed out before can be calculated by the formula, $T = \frac{\Delta\beta N A_{Fe}}{E}$, where $\Delta\beta$ depends upon the material in the core. Since the step length is directly proportional to $\Delta\beta$ it would seem best to have the maximum $\Delta\beta$ available during the step, and the minimum outside of the step. This immediately dictates the use of a new class of magnetic materials which have practically a rectangular hysteresis loop. The material which is now being used was originally developed in Germany by a subsidiary of Siemens as Permenorm 5000Z. This is essentially a 50% iron, 50% nickel alloy. The procedure for getting the optimum magnetic characteristics was brought over by Mr. Jensen and is discussed in some detail in Ref. (1). The Orthonol core used is an improvement of 5000Z developed by NOL and renamed Orthonol by them.

An exact determination of $\Delta\beta$ as discussed in Ref. (2) requires that the material actually be used in a rectifier to determine the maximum $\Delta\beta$. As a result, it is to a small extent a function of the ability of the contacts to open without arcing. In lieu of this, the value of $\Delta\beta$ in the knee of the saturation curve can be used. The best estimate of the actual position is of course made from experience but you can go further up as you increase the number of turns in the main winding since the magnitude of the current is still low. Fig. 17.

Improvement of the materials having rectangular hysteresis loops was delayed until a method was developed to

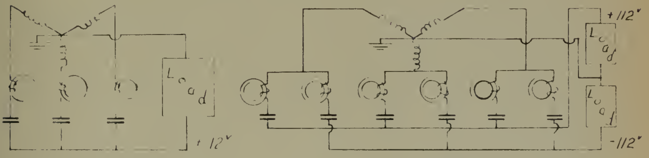
measure the characteristics under conditions approximating actual service. A DC loop, which was the conventional method, gave only a relative indication and was of no use as a basis for the calculations. The oscilloscope method is only approximate. As a result, no real progress was made until the Vectormeter was developed by Koppelman. This is essentially a small single phase mechanical rectifier which integrates to give the values of β and H versus an electrical angle. A complete description of its theory of operation is given by Koppelman (6). The circuit diagrams, pictures, and circuit constants of the one used for this test are included with the necessary calculations in Appendix I. These units were designed to work at 50 cycles but are performing satisfactorily at 60 cycles in this country.

CHAPTER II

[illegible] DESIGN CONSIDERATIONS

1. The Limitations imposed by Voltage and AC Source Inductance

The design of any rectifier must be based on the AC voltage available and the desired DC voltage. The AC voltage is normally transformed up, or down, to the necessary AC voltage to give the desired DC voltage. The maximum DC voltage available for any AC input is well known from mercury arc and selenium oxide rectifier design and applies equally well to the mechanical rectifier. Considering now only three phase input, the maximum DC voltage for a star pointed connection is 1.17 E_φ , and for a bridge connection, twice that or 2.34 E_φ, Fig. 11.



Star Pointed *Bridge*

Fig. 11

If a star pointed connection is contemplated, the effect of residual magnetism resulting from unidirectional flow of AC current must be considered, unless some such system as a zig-zag connected transformer is contemplated. The bridge connection would have no such disadvantage unless it were connected to a 3 wire system with a load unbalance. This connection also of course has a ripple frequency twice

that of star pointed connection, making it easier to filter.

For purposes of this investigation, it was apparent that from a weight point of view, in particular, it would be advantageous if no transformers were required. It is readily apparent then, that with the AC voltage of 120/208 volts available, a no load voltage of either 140 or 280 volts is possible without transformers. And, considering regulation, a full load voltage of either 112 or 220 volts could be obtained. With these two considerations in mind, there are two immediate answers, either the conventional one wire 112 volt system presently being considered for aircraft or; a two wire 112/224 volt system with a neutral structural return, would appear to the author to be well worth considering. This is particularly true since the negative side would be a ready source of negative bias in electronic equipment.

A bridge connection is only two, star pointed systems, put together so that for the purposes of this investigation, it was thought best to use a star pointed system for the following reasons:

1. It is the simpliest connection and lightest for a given power output.
2. It would give the DC voltage which is currently being considered for aircraft.
3. By simply combining two, it could be made into a bridge connection with double the power output and with no electrical redesign.

4. A larger enough alternator would be used so that this unit would draw less than 15% of the rated power output. This should make the residual magnetism effect small; probably too small to be objectionable.

5. The star pointed connection will have a greater overload capacity. This is true since the DC bus bars would be shorted out if both contacts were closed at the same time. (60 degree limitation) Fig. 10.

The line to line short circuit reactance of the AC supply system will affect the regulation of the rectifier. The AC transient analysis of an RL circuit, shows that the equation for the short circuit current is: $i_s = \dfrac{e_c(t)}{X_L}$. For any rectifier connection, $e_c = E_c\sqrt{2}(1 - \cos\omega t)$. ($E_c = 208^v$) This can be readily seen by referring to Fig. 12. Therefore:

$$(1) \quad i_s = \frac{E_c\sqrt{2}}{X_{Ls}}(1 - \cos\omega t)$$

The actual value of ωt will depend, of course, upon the value of load current, since commutation will be completed when $i_s = i_L$ as was shown previously.

This time, however, also results in a reactive voltage drop as a result of the voltage area loss, as was shown in Fig. 2. The voltage area loss can be found by integration to be:

$$(2) \int_0^t e_c\, dt = \frac{E_c\sqrt{2}}{\omega}(1 - \cos\omega t)$$

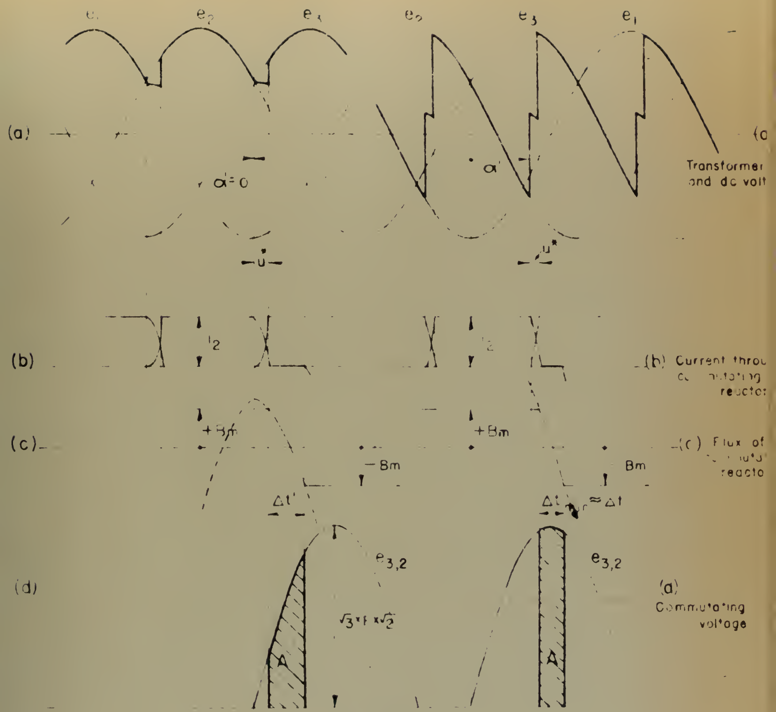
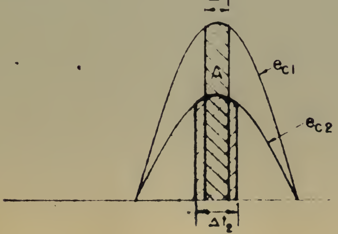


Fig 12

Full dc voltage

Low dc voltage



A consideration of Eqs. (1) and (2) will show that the lower the AC reactance, the larger the load current will be for a given output voltage, neglecting for the moment IR drop. The AC reactance then, is a definite limitation on the maximum power capacity.

It is now apparent that another reason for not using transformers is that the leakage reactance of the transformers will only add to the inductance of the system. For rectifiers working from a large power system, the transformer is the only source inductance which need be considered. However, for any system which will be used in aircraft in the forseeable future, the short circuit reactance of the alternator will be a large part of the total system inductance.

The actual value of short circuit reactance which should be used, is a variable, depending upon the loading of the machine, since $L = n\frac{d\varphi}{di}$. Of course the magnetic saturation curve gets flatter as the alternator is loaded up and the field current of necessity is increased. The Eclipse - Pioneer Division of The Bendix Aviation Corporation was kind enough to supply the calculated data on their 90 KVA alternator. The only reactance which is of interest in these calculations is the subtransient reactance. Rolf (3), Park and Robertson (5). This is true since the commutating time is .4 milliseconds at maximum overload. Unfortunately, all calculated data was computed on the basis of the air gap line (unsaturated). This would make the values used too large, just how much is a matter of

conjecture. In any event, to make this investigation conservative, the calculated value was used.

It can be easily seen from the limitations placed on the output DC voltage, that there is a maximum possible output capacity for a given AC source reactance. This can be varied to some extent by varying the inductance of the commutating cores main winding as will be seen in Chapter III. This is a fact which must be kept in mind as this paper is read, since it placed very definite limitations on the minimum weight of the cores.

There is also a minimum load for a mechanical rectifier. The load current must produce more ampere turns than does the pre-excitation. If this were not true, the step would occur at some unpredictable time. This would not necessarily be at the time that the contacts open. This would result then in a flash over. This minimum load will be called base load in this paper and will be discussed in more detail in Chapter IV.

2. Voltage Control

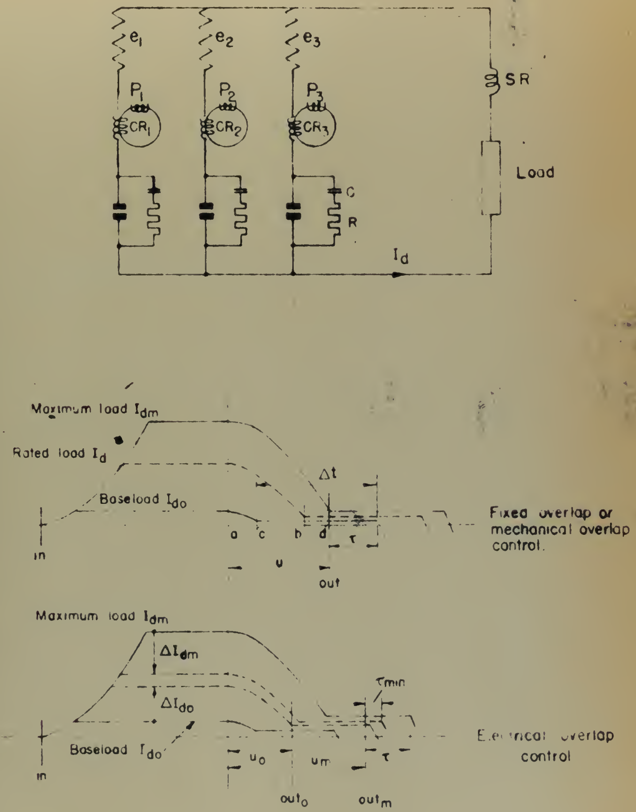
The two methods presently used for voltage regulation of a mechanical rectifier, neglecting a variable tap transformer, were discussed in the introduction. They accomplish the same thing, delay in the start of commutation. Some of the mechanical complexities of the mechanical system are readily apparent without dwelling further on the idea. The magnetic delay method is an easy system mechanically since the contacts continue operating as they did before. The disadvantage of such a system is that a more complex pre-excita-

tion system must be utilized and some compromise must be made with optimum pre-excitation. This is not a serious disadvantage, especially in small rectifiers with a large number of turns in the main winding. For a rectifier of this size, it is definitely a better system and so was used.

3. The Effect of Load Characteristics

Another consideration in determining whether a shift in contact closing time will be useful in reducing the size of the core is an evaluation of the load characteristics. A shift in opening time, will reduce the necessary step length of the breaking step since the opening time can be made to fall within the step by several methods, Fig. 13. However, this assumes that the load will change no faster than the contact closing time can be shifted. This is a poor assumption for any military installation. The resultant requirement for a long step length, merely increases the desirability of using the mechanical simplicity of magnetic delay.

It is possible, with all of the systems utilizing uni-directional current through the cores, to use only one core for both the make and break steps. This reduces the weight since it is not necessary to use a big make reactor too. The other system using bi-directional current and therefore two contacts for each core was not even considered in this investigation since the overload limitations are very severe, and magnetic delay cannot be used.



e1
e2
e3
P1
P2
P3
CR1
CR2
CR3
SR
Load
C
R
Id
Maximum load Idm
Rated load Id
Baseload Ido
Δt
a
c
b
d
τ
in
u
out
Fixed overlap or mechanical overlap control.
Maximum load Idm
ΔIdm
ΔIdo
τmin
Baseload Ido
in
u0
um
τ
out0
outm
Electrical overlap control

Fig 13

CHAPTER III

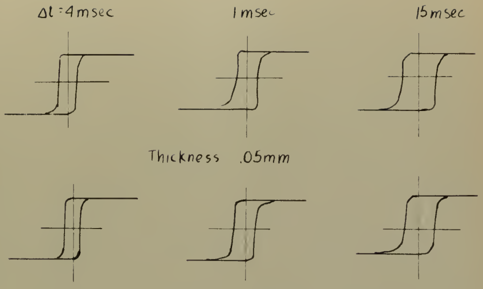
SELECTION OF A CORE

1. Experimental Data

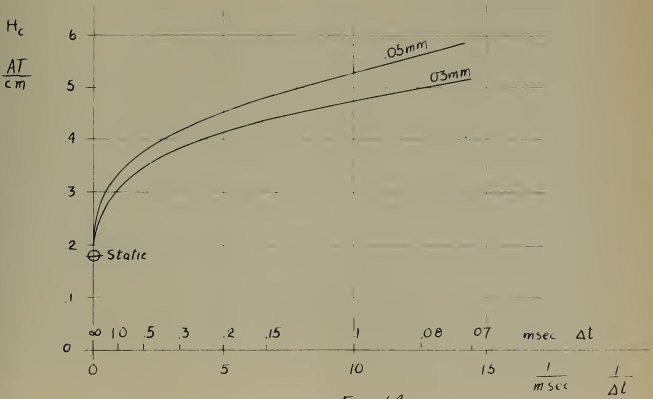
Reasonable assumptions on the saturable core characteristics had to be based on some sort of data. All quantitative information available in the literature, was for step lengths of 1 millisecond and longer and for ribbon thicknesses of .03 millimeters (1.2 mils) and thicker.

Dr. Rolf (3) gave some relative information on losses versus step length and ribbon thickness which is included in this report as Fig. 14. This information is for 5000Z but should closely approximate presents results using Orthonol.

This figure indicated that the thinnest ribbon currently available would be best to use for this investigation. The use of such thin ribbon results in other problems. The primary difficulty is the actual rolling of the ribbon and the winding of the core. Another adverse factor is the decrease in the maximum flux density for saturation as the ribbon is made thinner. There is some reason to believe at the present time that this may be the effect of surface impurities. That is merely conjecture, however, and any confirmation will require more investigation. If this be true, current research by NOL on saturable cores may solve this problem. In this case, improvement for use in magnetic amplifier applications also means improvement for use in a mechanical rectifier.



Δt = 4 msec
1 msec
15 msec
Thickness .05mm
Thickness .03mm



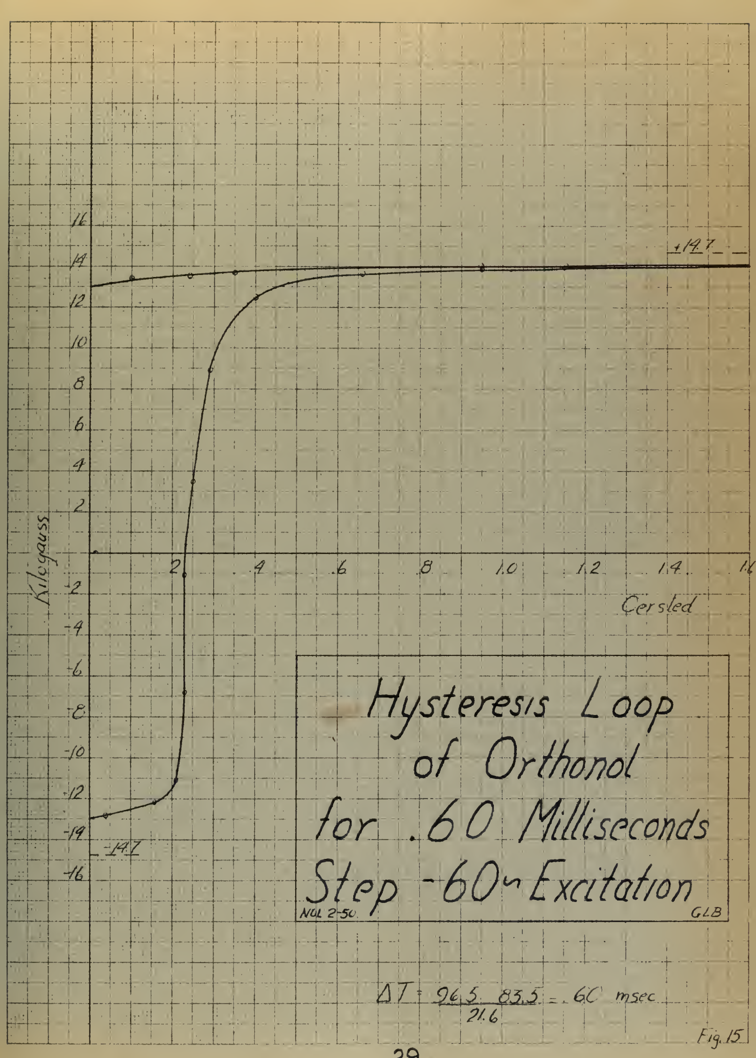
H_c
$\frac{AT}{cm}$
.6
.5
.4
.3
.2
.1
0
Static
.05mm
.03mm
∞ 10 .5 .3 .2 .15 .1 .08 .07 msec Δt
0 5 10 15
$\frac{1}{m\,sec}$
$\frac{1}{\Delta t}$

Fig. 14

The Vectormeter appeared to be the only method to obtain accurate quantitative data. The data obtained from this unit at 60 cycles could be used as a reference to obtain the hysteresis loop for 400 cycles by comparing the two under identical conditions. The methods used in this paper gave hysteresis plus eddy current losses, even though they will be referred to as hysteresis loops throughout this paper. This was especially attractive since it was anticipated that there would be little difference between the loops for the two frequencies for a given step length. The results are shown in Figs. 15, 16, 17, and 18.

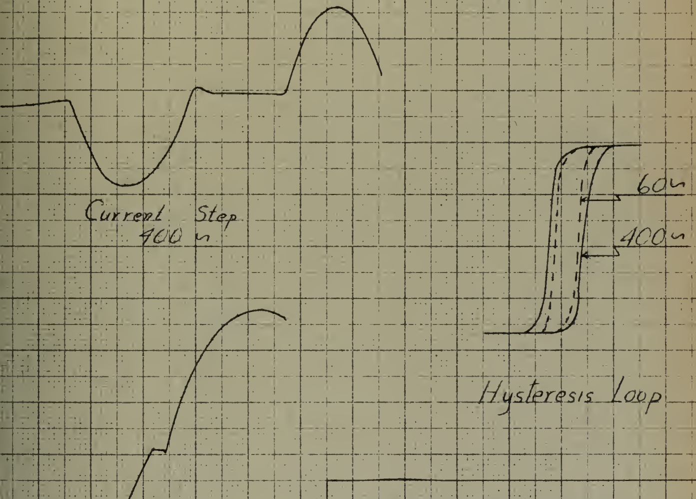
It was anticipated that the eventual design step length would be between .6 and .3 milliseconds and so the comparisons were made at those frequencies. The circuit used is shown in Appendix I.

The experimental core furnished by the Magnetic Amplifier Section of NOL consisted of 190 turns of Orthonol ribbon .0006" x .25". It had its final heat treatment in a constant magnetic field of 87.2 oersteds at 300°C. This was the only variation from the method described by Jensen (1). The DC loop shown in Fig. 19 was obtained with the conventional ballistic galvanometer method by NOL personnel. The hysteresis loops for approximately 1.2 and .2 milliseconds are given in Appendix I. The loop for a .2 milliseconds is only approximate. It appears to be about the shortest step length for which this model Vectormeter can be used. Periodic difficulties were encountered during the actual operation as a result of fluctuations in the line



Hysteresis Loop of Orthonol for .60 Milliseconds Step -60~ Excitation

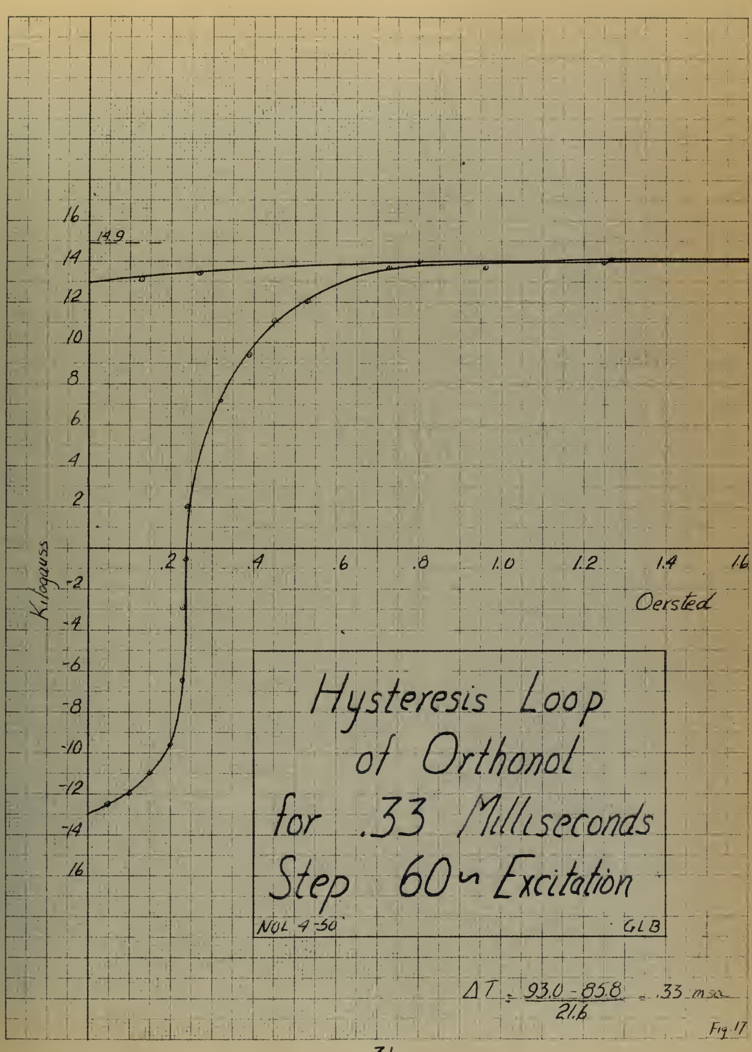
Fig. 15

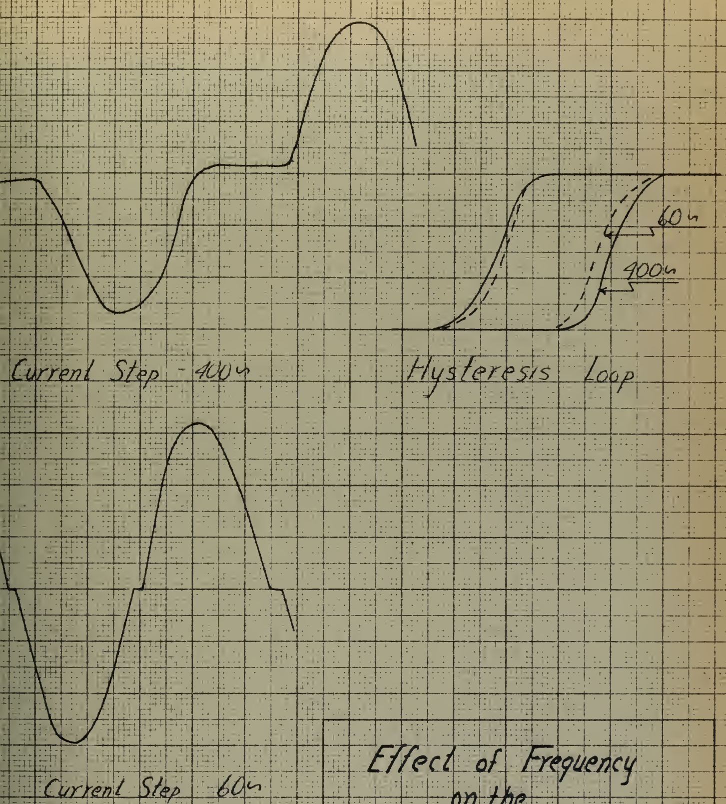


Effect of Frequency
on the
Hysteresis Loop of Orthonol
for a
Step Length of .6 millisecond

NOL 2-50 GLP

Fig 16

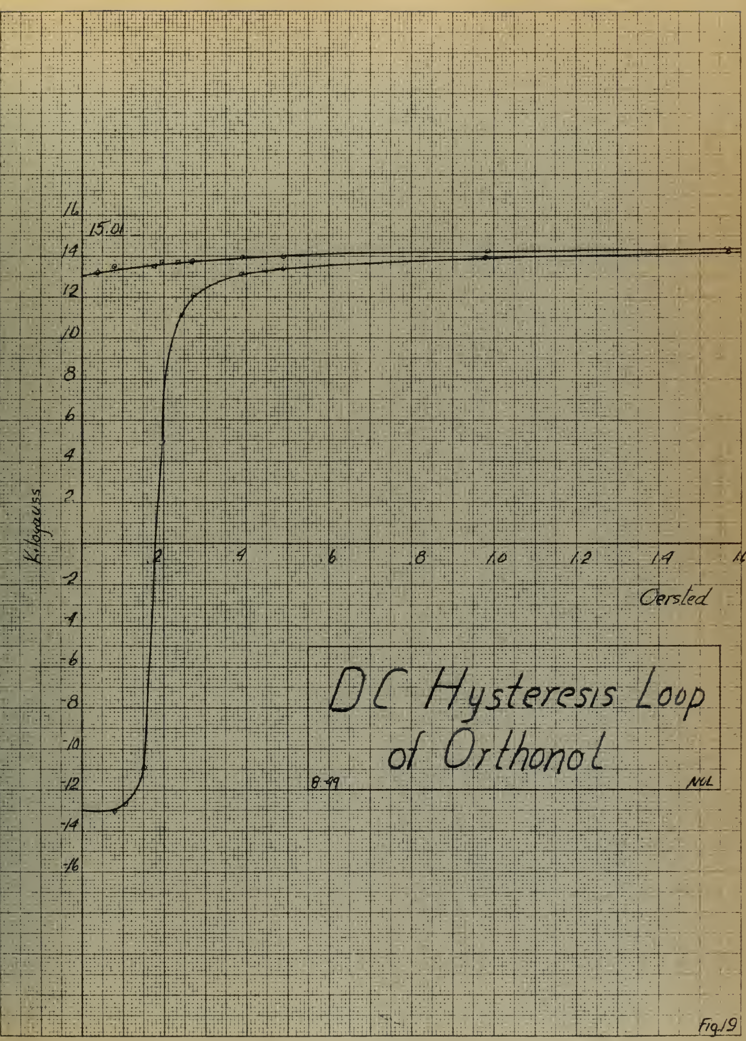

16
14.9
14
12
10
8
6
4
2
-2
-4
-6
-8
-10
-12
-14
16
Kilogauss
.2
.4
.6
.8
1.0
1.2
1.4
1.6
Oersted
Hysteresis Loop
of Orthonol
for .33 Milliseconds
Step 60 ~ Excitation
NOL 4-50
GLB
ΔT = (93.0 - 85.8) / 21.6 = .33 msc
Fig. 17



Effect of Frequency
on the
Hysteresis Loop of Orthonol
for a
Step Length of .30 Milliseconds

NOL 4-50 GLB

Fig 18


16
15.01
14
12
10
8
6
4
2
-2
-4
-6
-8
-10
-12
-14
-16
Kilogauss
.2
.4
.6
.8
1.0
1.2
1.4
1.6
Oersted
DC Hysteresis Loop of Orthonol
8-49
NOL
Fig.19

voltage but beyond that, no difficulty was encountered.

A $\Delta\beta$ of 26 kilogauss was selected from the data obtained. This may appear a little optimistic on the basis of the loops actually obtained. However, the saturation value was about .4 kilogauss lower than would normally be expected on the basis of data on other cores. This is of no great concern since any overestimate would only result in a slightly optimistic figure for overload. The error certainly is not more than 2 kilogauss or 8%. This could be easily more than counterbalanced by the overestimate in the reactance of the alternator.

The determination of the required step length at this point in the investigation is at best, a shot in the dark. There are several formulae available in different forms for this calculation. The one developed by Koppelman (2) in his original series of articles on the subject seems the best to this author. This is particularly true since it emphasizes the factors which influence the required step length, and so the weight, power output, and all of the other factors considered previously.

A photostat of the pertinent part of this series, showing the derivation of the step length formula, is included as Appendix II to this report.

2. The Determination of Probable Step Length.

A study of the characteristics which this rectifier will probably have shows that the critical loads will be rated load and maximum overload. This is undoubtedly not apparently to the reader unless he has had considerable

experience with these units. However, a little thought will show that up to rated load, there is some voltage control. This will cause a delay in the start of commutation, and as a result, a delay in the start of the step. Considering now IR drop, the difference in voltage area used up for commutation at rated load and that used up for voltage control and commutation at any lower load will be the additional IR drop, if the output voltage at the two loads is the same. For that reason, the step will start sooner at rated load than at any other load. This assumes of course that the rectifier has been designed so that it will just deliver rated voltage at rated current. If the design is too conservative, the critical load will be at some overload value and for an optimistic design, at a value below rated load current. In the latter event, the rating of the unit would have to be reduced.

The following is a preliminary evaluation of the required step length, using Koppelman's formula for step length.

From Eq. (1) $i_s = \frac{E_c\sqrt{2}}{X_{L_s}}(1-\cos\omega t)$

This becomes, for rated load

$$X_{L_s} = \frac{E_c\sqrt{2}}{I_L}(1-\cos\omega t)$$

Also, from Eq. (2)

$$\int_0^t e_c\,dt = \frac{E_c\sqrt{2}}{\omega}(1-\cos\omega t)$$

Substituting the value of X_{L_s} to obtain rated voltage at rated load, the maximum value can be shown to be:

$$X_{L_s} \leq \frac{\omega \int_0^t e_c\,dt}{I_1}$$

or:

$$(3) \quad L_s \leq \frac{\int_0^t e_c\,dt}{I_1}$$

By integration, the total voltage area for one cycle, assuming no voltage loss from commutation or delay, is:

$$E_\varphi \sqrt{2} \int_{30}^{120} \sin \omega t \, dt = \frac{E_\varphi \sqrt{6}}{\omega} = \frac{120\sqrt{6}}{800\,\pi} = .117 \text{ volt secs.}$$

This is equivalent to 140.4 volts DC.

Voltage $= \dfrac{.117 \times \omega}{\frac{2\pi}{3}} = \dfrac{.117 \times 800\,\pi}{\frac{2\pi}{3}} = .117 \times 1200 = 140.4^v$

Considering now the rated load voltage, 115 volts in this case, there is a definite amount of voltage area which can be used up in commutation. Also some allowance must be made for IR drop. For a unit of this size, with a current density of about 125 circular mils per ampere, a reasonable estimate of IR drop is 4 volts. Therefore, the reactive voltage drop must be not more than:

$$140 - (115 + 4) = 21 \text{ volts}$$

This represents a voltage area of:

$$\frac{21}{140.4} \times .117 = .01756 \text{ volt secs.}$$

Using Eq. (3) and knowing that $I_1 = \frac{9,600}{115} = 83.5^a$

$$L_s \leqslant \frac{.01756}{83.5} = 210\ \mu h$$

Looking at the hysteresis loop, the core is not completely saturated until the mmf is about 20 oersteds. Therefore, there will be a finite delay resulting from this $\Delta\beta$. The actual voltage area can be computed by a method which will be shown later. This proves to be equal to .00656 volt seconds. This then places a maximum limit on L of:

$$L_s \leqslant \frac{.01756 - .00656}{83.5} = 132\ \mu h$$

Koppelman's formula must be modified slightly to be used with the reasoning advanced at the beginning of this section. Fig. 20 shows the time actually obtained by this method:

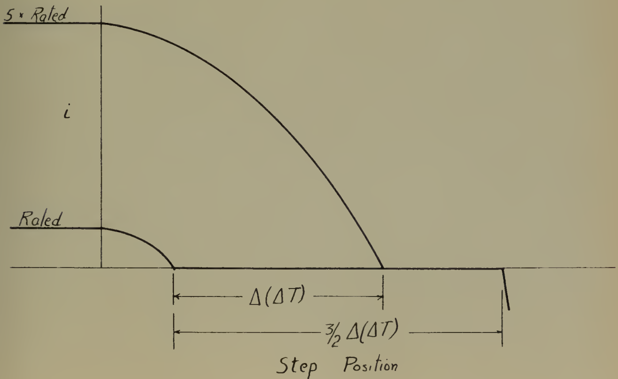


Fig. 20

Koppelman's original formula was:

$$\Delta T = \frac{\mathcal{E} U}{\omega(1 - \sigma - \gamma - \delta)}$$

$$\mathcal{E} = \frac{\omega L_s I_1}{E_c \sqrt{2}} = \frac{800\pi \times 132 \times 10^{-6} \times 83.5}{208\sqrt{2}} = .0941$$

$$\gamma = \frac{2(B_{max} - B_{in})}{\Delta B} = \frac{2(1.50 - 1.31)}{2.6 \times 10^{4}} \times 10^{-4} = .0146 \text{ (Saturation Delay)}$$

$$\sigma = \frac{\Delta B_{out} - \Delta B_{\sigma}}{\Delta B} = .33 \text{ by assumption (safety step)}$$

$$\delta = \frac{(B_{Rated\ Load} - B_{Base\ Load})}{\Delta B} = \text{negligible using Orthonol}$$

U - over load factor (1 at rated load)

Referring again to Fig. 20, the safety step is of interest only for the minimum commutating time; this is the normal rated load in this case. The saturation delay of course enters into both considerations since it delays commutation under all conditions. The value of $\Delta(\Delta T)$ then becomes:

$$(4) \quad \Delta(\Delta T) = \frac{\mathcal{E}}{\omega(1-\gamma)} \left\{ U_{max\ Load} - U_{rated\ load} \right\}$$

The value for maximum load rating was increased 25% to take care of manufacturing discrepancies, frequency shifts, voltage fluctuations, any over estimate of $\Delta\beta$, and an over simplification of a complex problem. This then gives a value for $\Delta(\Delta T)$ of:

$$\Delta(\Delta T) = \frac{.0941}{800\pi(1-.0146)} \left\{5 \times 1.25 - 1\right\} = .199 \text{ milliseconds}$$

$$\text{Therefore } \frac{3}{2}\Delta(\Delta T) = .299 \text{ milliseconds}$$

For this investigation, .305 milliseconds was used. From Jensen (1)

$$NA_{Fe} = \frac{\Delta T E_c \sqrt{2}}{\Delta\beta} = \frac{.305 \times 294}{2.6 \times 10^{-4}} = 346$$

Final calculations using this value gave an overload rating of 5.90 x rated current or 18% instead of the 25% originally designed for. This method appears to give a very accurate and simple method of rapidly computing the value of NA_{Fe}.

It is now possible to show the method which was used to determine the voltage area resulting from the saturation delay. Since we know the value of $\Delta\beta$ and NA_{Fe}, the following formula can be derived:

$$e = N\frac{d\varphi}{dt} \quad \text{or} \int_{t_1}^{t_2} e\,dt = N \times (\varphi_2 - \varphi_1)$$

$$= N \times A(\beta_2 - \beta_1)$$

$$(5) \qquad \int_{t_1}^{t_2} e\,dt = NA_{Fe}\,\Delta\beta$$

$$= 346 \times 19 \times 10^{-4} = .00656 \text{ volt secs.}$$

This then gives the voltage area which was used in the step length calculations. The remaining $\Delta\beta$ of .01 x 10^{-4} will be taken care of by another circuit.

3. The Determination of the Optimum Core Area

The preceeding analysis gave an excellent starting point for the actual determination of the optimum core area, since one variable, NA_{Fe} has been eliminated from the calculations. There are, of course, an infinite number of combinations which will give this product. However, the design output voltage is still the determining factor. This then limits the maximum inductance of the entire short circuit path.

This limitation immediately suggests that we first consider the reactance over which there is no control. This is the inductance of the source. One of the initial conditions imposed was that there would be no transformer used. The rectifier will probably be mounted next to the alternator so that the source reactance, is the reactance of the alternator. This reactance is the subtransient reactance as was discussed in Chapter II. The value supplied by Eclipse for their 90 KVA alternator was 33% for the direct and quadrature axis (unsaturated) at 320 cycles. Using the method advanced by Park and Robertson (4), the following results were obtained:

$$\text{Normal ohms} = \frac{\text{normal line to neutral voltage}}{\text{normal line current}}$$

$$= \frac{208}{\sqrt{3}} \times \frac{208\sqrt{3}}{90 \times 10^{3}} = .481\,\Omega$$

Subtransient reactance = 33% at 320

$= .33 \times .461 = .159\,\Omega$ at $320 \sim$

Therefore $L_a = \frac{.159}{\omega} = \frac{.159}{2\pi 320} = 79\ \mu h$

This means then that the maximum inductance of the main winding of one core $\leq \frac{132-79}{2} = 26.5\ \mu h$

We now know the maximum allowable inductance of the main winding, the value of NA_{Fe}, and the only other limiting factor is the value of $\frac{2W}{d_m}$ (Fig. 21). Gaugler (5) has shown that the slope of the side of the hysteresis loop decreases as the value of $\frac{2W}{d_m}$ increases. This slope can be represented by the relation:

$$\Delta H_m = H_c \ln \frac{r_o}{r_i} \qquad \text{Fig. 22}$$

The optimum value of $\frac{2W}{d_m}$ is a complete problem in itself. For this investigation, the same value was used as was used in the sample core to insure reasonable results, 0.414. It was a reasonable value, however. Dr. Rolf (3) stated that the value varied from 0.25 to 0.45 in German rectifiers and from 0.4 to 0.65 in the rectifiers built so far in this country. A larger value would have meant a lighter core at the sacrifice of slope.

The only other factor needed was stacking factor. It was very low as a result of the thin ribbon used. Again, the value obtained from the sample core was used, 0.340.

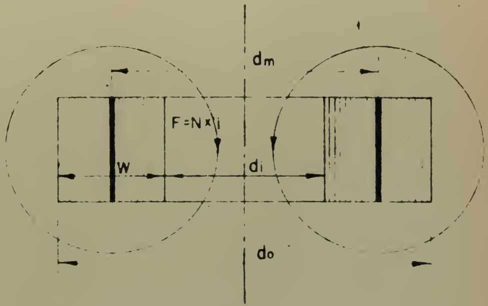


Fig- 21

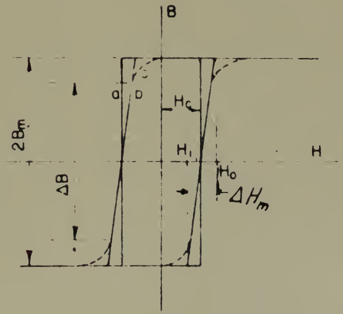


Fig-22

The final attempt to insure that the core would have the same characteristics as the sample was made by making the core up out of layers of individual cores the same height as the sample core, $\frac{1}{4}$ inch. This introduces problems in matching which will be primarily production problems. The effects of poor matching will be discussed later. It may well be necessary, however, to use these thin sections with the short step lengths used, to reduce the effect of eddy currents on the size of the hysteresis loop.

All of these boundary conditions were combined as a basis for calculations which resulted in Fig. 23. As an example of the method used, the calculations for the core finally selected are as follows:

$$A_{Fe} = \frac{346}{70} = 4.95 \text{ sq cms.}$$

$$A_{core} = \frac{4.95}{.340} = 14.6 \text{ sq. cms}$$

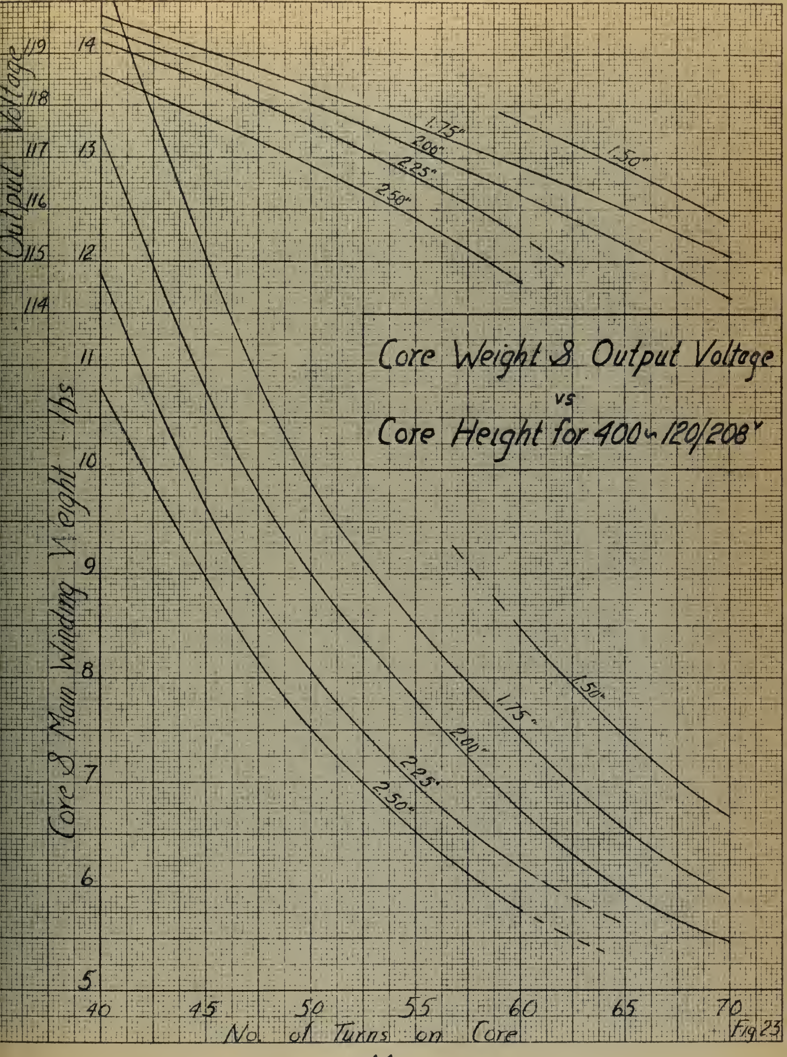
$$\text{Height} = 7 \times 1/4 = 1.75 \text{ in. or } 4.45 \text{ cms.}$$

$$\text{Thickness} = \frac{14.6}{4.45} = 3.28 \text{ cms.}$$

$$d_m = \frac{2 \times 3.28}{.414} = 15.85 \text{ cms.}$$

$$OD = 15.85 + 3.85 = 19.33 \text{ cms or } 7.62 \text{ inches.}$$

The calculation of the inductance of the main winding required that the area enclosed by one turn be determined. For this, a spacing of .15 cms. was allowed for each


Core Weight & Output Voltage
vs
Core Height for 400~ 120/208Y
Output Voltage
Core & Main Winding Weight - lbs
No. of Turns on Core
119
118
117
116
115
114
14
13
12
11
10
9
8
7
6
5
40
45
50
55
60
65
70
1.50"
1.75"
2.00"
2.25"
2.50"
Fig 23

individual layer for potting, supports, and outside insulation. The radius of the copper of the winding was neglected since it was small as compared with the assumptions for the spacing.

$$A_t = (4.15 + 7 \times .15)(3.28 + 2 \times .15) = 19.7 \text{ cms.}^2$$

Dr. Rolf (3) recommended that the theoretical air core induction for a torodial core be multiplied by a factor of 1.05 so that the difference in spacing between the turns on the inside and outside of the core would be considered. The air core induction was used since the effect of the iron core is negligible during commutation. ($\delta = 0$) The formula then becomes:

$$L = \frac{4.2 N^2 A_t}{d_m} = \frac{4.2 (70)^2 \times 19.7}{15.85} = 25.6 \text{ uh}$$

Therefore $L_s = 79 + 2 \times 25.6 = 130.2$ uh

And from Eq. (3)

$$\int_0^t e_L \, dt = I_1 L_s = 83.5 \times 130.2 \times 10^{-6} = .01088 \text{ volt secs.}$$

The voltage area for the saturation delay was: .00656 volt seconds.

Therefore the net voltage area was:

$$.117 - (.01088 + .00656) = .09956 \text{ volt secs.}$$

This gives a reactive voltage of:

$$.09956 \times \frac{\omega}{\frac{2\pi}{3}} = .09956 \times 1200 = 119.6 \text{ volts}$$

The core weight can also be determined, again using the test core.

$$\frac{4.95}{.205} \times 24.4505 \times 2.205 \times 10^{-3} \times \pi \times \frac{15.85}{14.46} = 4.50 \text{ lbs}$$

The core loss can be determined directly from the hysteresis loop area. The hysteresis loop for .30 milliseconds was used for the calculations. Actually, the step will never be quite that short since the commutating voltage will never be an average of $E_c\sqrt{2}$ as was assumed in obtaining this value. However, this will give a conservative answer.

Area of hysteresis loop $= \int B\, dH$ for B in gauss and H in AT/cm, and ρ in grams/sq. cm.

$$a = \int B\, dH = \frac{10^8 \text{ Vsec}}{cm^2} \times \frac{AT}{cm} = \frac{10^8 \text{ Watt sec}}{cm^3} \quad \text{or } W = a \times 10^{-8}$$

This will give a loss per gram of:

$$\text{Loss/gram} = \frac{W sec}{cm^3} \times \frac{1}{sec} \times \frac{cm^3}{g} = \frac{W}{g} = \frac{a \times freq}{\rho} \times 10^{-8}$$

$$(6) \quad \text{Loss/lb.} = \frac{a \times freq \times 10^{-8}}{\rho \times 2.205 \times 10^{-3}}$$

The .30 millisecond step at 60 cycles (Fig. 17) has a value of a = 30,600 $\frac{\text{Wsec.}}{cm^3}$ Fig. 18 shows that the ratio of areas is 1.2. Therefore the area for 400 cycles for a .30 millisecond step is:

$$50.6 \times 10^3 \cdot 1.2 = 36.8 \times 10^3 \ \frac{W\,sec}{cm^3}$$

Therefore $W/lb. = \dfrac{36.8 \times 10^3 \times 400 \times 10^{-8}}{8.25 \times 2205 \times 10^{-3}} = 8.1$ watts / lb.

And the core loss is 8.1 x 4.50 = 36.4 watts.

Returning now to the main winding copper. If a current density of 125 circular mils per ampere is used, this gives a cross sectional area of 125 x 83.5 = 10,430 circular mils. Number 10 gage wire has a cross sectional area of 10,400 circular mils, therefore this was selected for the main winding. The Bureau of Standards Circular #31 lists the following physical constants for #10 wire:

Resistance = 1.18 ohms/1000 ft at 65° C.

Weight = 31.47 pounds/1000 feet

Allowing 48 inches for external connections, the copper length will be, neglecting advance per turn:

$$\frac{(2 \times 5.50 + 2 \times 3.50)}{2.54} \times 70 + 48 = 548 \text{ inches}$$

$$\text{Copper weight} = \frac{548 \times 31.47}{1000 \times 12} = 1.44 \text{ pounds}$$

$$\text{IR drop} = \frac{548 \times 1.18 \times 83.5}{1000 \times 12} = 4.48 \text{ volts}$$

$I^2R = 4.48 \times 83.5 = 376$ watts for 120° out of 360°.

Therefore, average $I^2R = 125$ watts.

4. Final calculation of step location and length.

Now that all of the characteristics of the core are known, it is possible to determine where the step will be located for any load. This will make it possible to

calculate how much overlap there should be and what the maximum overload will be. Effects of frequency and voltage fluctuations can also be determined.

The following is a sample calculation to show how the data for Fig. 24 was calculated.

Load - $\frac{1}{2}$ rated

$I = 41.7$ amps.

IR drop = $.0538 \times 41.7 = 2.24$ volts

Reactive voltage = $115 + 2.24 = 117.24$ volts

Voltage area for 115 volts = $\dfrac{.117 \times 117.24}{140} = .0980$ Volt secs.

Reactive voltage area drop = $.117 - .0980 = .019$ volt secs.

Voltage area to saturate core = $.00656$ volt secs.

Commutation voltage area = $41.4 \times 130.2 \times 10^{-6} = .00538$ volt secs.

Voltage area for magnetic delay = $.019 - (.00656 + .00538)$
$= .00706$ volt secs.

From Eq. (2)

$$\cos \omega t = 1 - \frac{\omega \int e_L \, dt}{E\sqrt{2}} = 1 - \frac{800\pi \times .00706}{294}$$

$\omega t = \alpha = 20.1^\circ$

Therefore, the make step will last 20.1° after the contact has closed.

The current will complete commutation after:

$$\cos \omega t = 1 - \frac{800\pi \times .019}{294}$$

$\omega t = 33.2^\circ$

33.2° after the contact has closed.

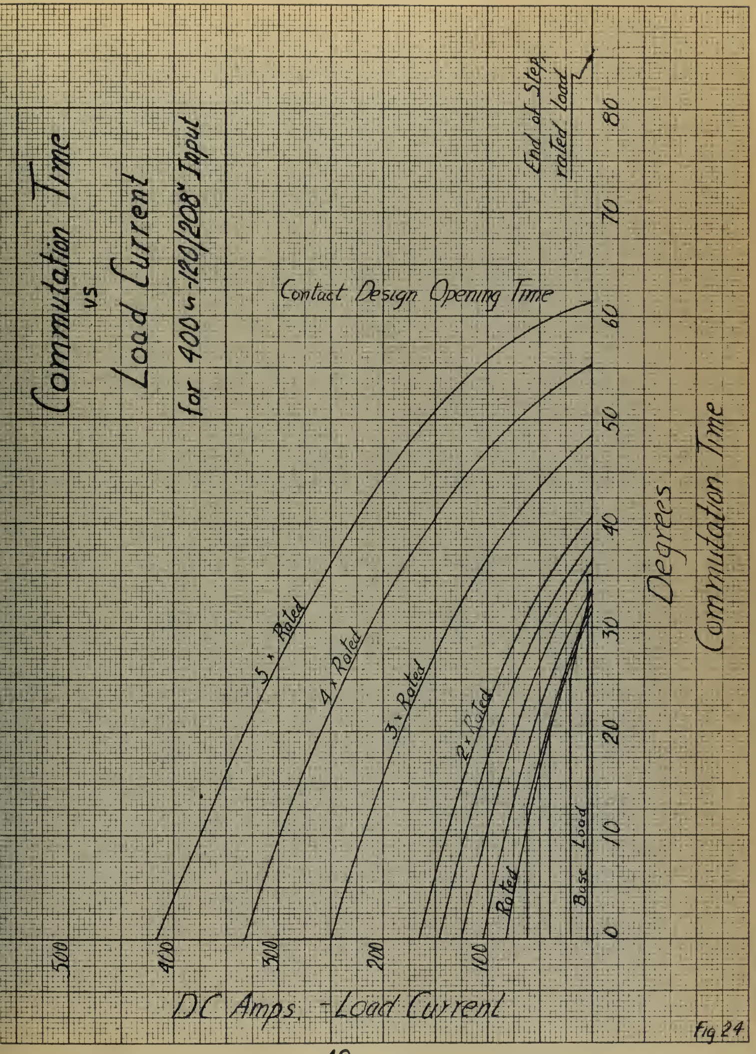

Commutation Time
vs
Load Current
for 400~-120/208V Input
Contact Design Opening Time
End of Step
rated load
5 x Rated
4 x Rated
3 x Rated
2 x Rated
Rated
Base Load
0
10
20
30
40
50
60
70
80
Degrees
Commutation Time
100
200
300
400
500
DC Amps. - Load Current

Fig 24

This angle also determines when the decay is completed and the break step starts. Therefore, the break step will start 120 + 33.2° or 153.3° after the contact closed.

The equivalent voltage area of the break step can be calculated by Eq. (5)

$$\int e\,dt = NA_{Fe}\,\Delta B = 346 \times 2.6 \times 10^{-4} = .0900$$

$$\text{Therefore:} \quad \cos \omega t = \cos 32.2^{\circ} - \frac{800\pi \times .0900}{294}$$

$$\omega t = 86.5^{\circ}$$

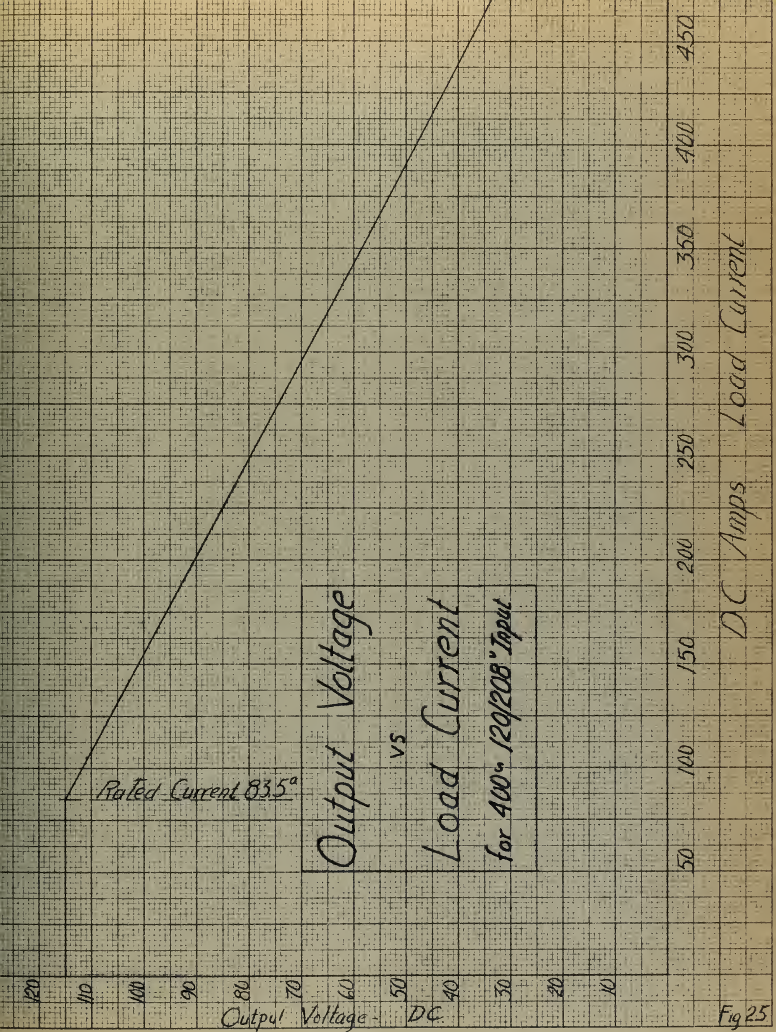
Therefore, the break step will last until (120 + 86.5) or 206.5° after the contact closes. The values for the other loads are shown in Fig. 24. This shows that the maximum load this rectifier can carry without moving into the safety step is: 5.90 rated current.

Fig. 24 shows that the critical load is rated load as was predicted early in this Chapter. This load was used then to determine the proper overlap setting. Allowing a safety step of 1/3rd the total step, the contact must be set to open at 67.5° overlap or 187.5° after the contact closed. However, the contacts should be set for 61.3° or 5 times rated current. This will make the unit less sensitive to overvoltage and under frequency fluctuations.

Fig. 25 is a plot of output voltage versus load current. As can be seen, the mechanical rectifier is largely self limiting.

5. The Effect of Frequency and Voltage Fluctuations.

The design thus far has assumed a constant frequency of 400 cycles and a constant input voltage of 120/208 volts


Output Voltage
vs
Load Current
for 400~ 120/208V Input
Rated Current 83.5a
DC Amps Load Current
50
100
150
200
250
300
350
400
450
Output Voltage - DC
120
110
100
90
80
70
60
50
40
30
20
10
Fig 25

AC. Aircraft installations in particular cannot be expected to maintain any frequency or voltage exactly, no matter what sort of drive is contemplated.

There are several different frequency ranges now in the specifications. The most limited of these is the range of from 380 to 420 cycles for the constant speed drive. Considering this range first. An analysis in the same manner as was done for 400 cycles but only for the critical values shows that a rectifier with this long a step length has surprising flexibility.

380 cycles - Output voltage - maintained to 105% rated load

Maximum overload - Increased about 5%

This results since there is more voltage area and so the unit can carry more current before the reactive voltage drops to the minimum allowable value. Also, for maximum overload, there is more time for commutation. By the same reasoning:

420 cycles - Output voltage - maintained to 95% rated load

Maximum overload - reduced about 5%.

If we now try to put this rectifier on any engine driven alternator and assume that some one has designed a contact mechanism which will actually work at 930 cycles:

320 cycles - Output voltage - maintained to 125% rated load

Maximum overload - 170% of 400 cycles value

930 cycles - Output voltage - maintained to 50% rated load

Maximum overload - 175% rated load

Voltage at this rating only 61 Volts, however.

Now considering the affects of voltage fluctuations at 400 cycles, only, since other values could be readily obtained. Assuming a phase voltage of 150 volts, the critical load will be in the normal operating range, but the safety step will be reduced, however, it will still be 9×10^{-5} seconds for 67.5^{o} overlap which is more than the minimum specified by Koppelman (2). This should only be a transient condition anyhow. This condition can be improved still more if the overlap is reduced to 61.3^{o} or the equivalent of 5 x rated current at 400 cycle.

Assuming now a phase voltage of 80 volts, the critical load will exist in the overload region. At this voltage, the overload will be limited to 300% rated load. However, the output DC voltage for such a condition would be only 15 volts anyhow.

This has not been a complete analysis of the effect of transients on the performance of this rectifier; it should give an indication of the flexibility possible with this size core.

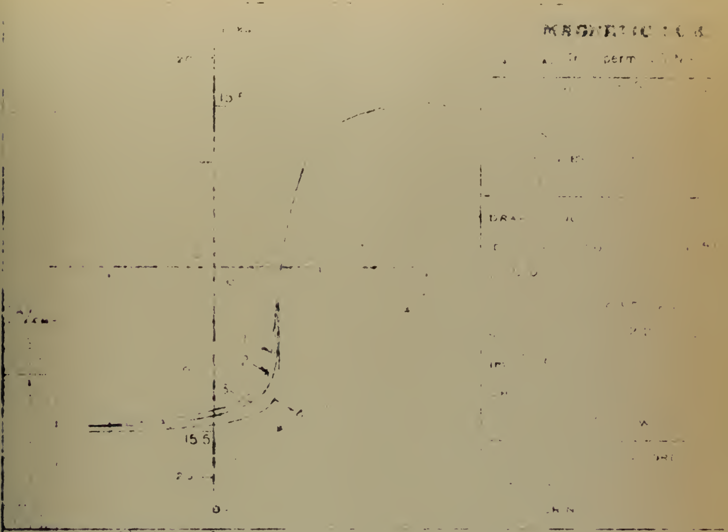
5. Manufacturing and Handling Problems:

Before leaving the subject of saturable cores, it would probably be wise to mention some of the problems which are currently disadvantages and some which will probably always

be a disadvantage.

Any core which is made up of a number of individual ones such as this design envisions will be a severe production problem since they must all have almost the same size hysteresis loop to avoid a composite loop looking like Fig. 26. This is a severe handicap at the present time since there is no assurance just what size loop a given core will have until it is tested on the Vectormeter. This can probably be overcome by more careful control after more has been learned about the heat treatment.

It is also extremely important that the core be subjected to no physical stress. If the elastic limit is exceeded, the magnetic qualities will be ruined and the core will have to be junked. Any stress which does not exceed the elastic limit can result in changes such as are shown in Fig. 27. This means that there must be sufficient room inside the aluminum can in which the core is housed for its thermal expansion. Also, it might be necessary to support part of the core separately as shown in Fig. 28 if the effects of high "g" and vibration had too great an effect on the hysteresis loop.



REMARKS 25% Silicon Iron

$H_m = 10$ [illegible]

Stack of 4 cores, each 15mm high

Core assembled with (1) washers tightened

(2) washers removed

(3) twine bands removed

(4) pre-exitation windings removed

Fig **27**

8-4

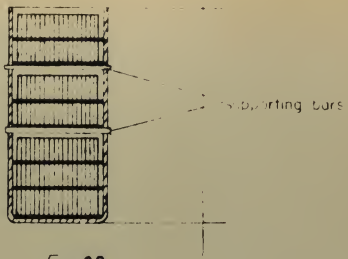


Fig 28

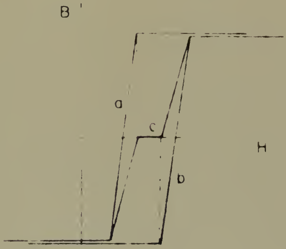
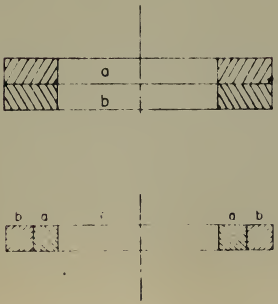


Fig 26



CHAPTER IV

PRE-EXCITATION

1. General

The requirement of this rectifier that there be a magnetic voltage control system automatically dictated two separate and complete pre-excitation systems. The first, the AC system, is the one described in the Introduction. It is always required to insure positive current flow through the contacts as they open, for the reasons outlined there. In addition to this, a DC pre-excitation is required with magnetic voltage control to control the amount of magnetism of the core when the contact closes. As a result, the DC winding is put on so that it neutralizes the effect of the AC winding during the make step and aids during the break step. This is a disadvantage during the break step, however, since it results in a variation of the current flowing through the contacts at the break as a function of load current. (Voltage control)

The core acts as a conventional transformer during the step and as a result, some impedance must be placed in the line to keep the pre-excitation current at the desired value. This could be a resistor but this would involve large losses. For this reason, an inductance is much more satisfactory.

The inductance in the AC pre-excitation circuit not only limits a transient change in current during the step but also limits the amount of current flowing. This double

requirement makes the AC pre-excitation very inflexible and as a result, it will be considered first.

2. AC Pre-excitation.

There are two simple connections possible for AC pre-excitation, Y, Fig. 29 or Δ , Fig. 30. The phase relations for the two connections are shown in Fig. 31 for the break step and Fig. 32 for the make step. As can be seen, the optimum is between the two connections. For this design,- the Δ connection seemed better since it supplied more ampere turns prior to the time the contacts closed. This in turn results in a higher flux density and less saturation delay at rated load and above. It does have the disadvantage, however, that during the break step, the pre-excitation begins to decrease when it should be still increasing. The increase would be an advantage, since the step is shorter at the higher loads. This is true because the effective value of the commutating voltage is larger. This results in a larger hysteresis loop and so more ampere turns.

The double requirement for the inductance, in effect, dictates how many turns of pre-excitation can be used on the core. The equation can be derived to solve this problem as follows:

Let z be the per unit change in pre-excitation current.

$$z = \frac{i_p}{I_p} \qquad \text{(Fig. 33)}$$

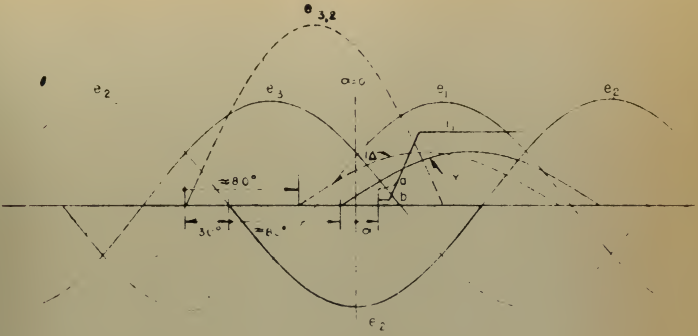


Fig 32

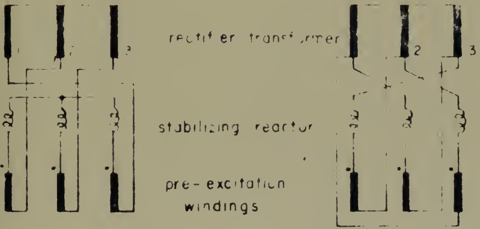
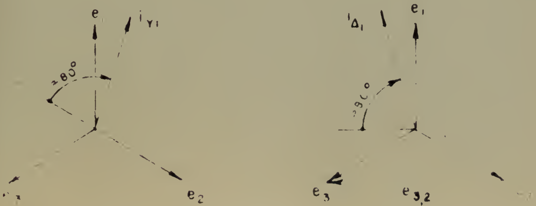
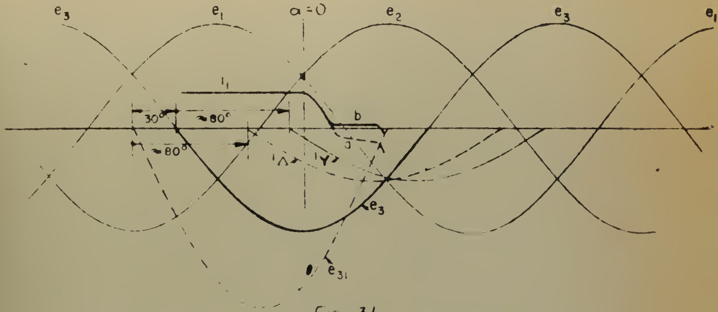


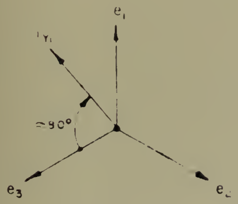
Fig 29 Fig 30



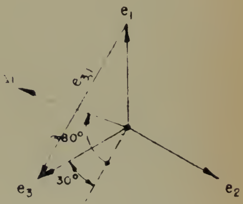


e3
e1
a=0
e2
e3
e1
i1
b
30°
≈80°
≈80°
e3
e31

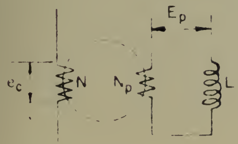
Fig 31



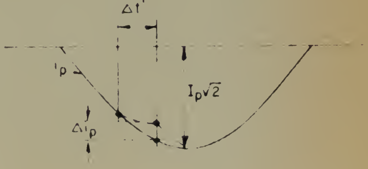
e1
≈90°
e3
e2



e1
e31
≈80°
30°
e3
e2



Ep
ec
N
L



Δt'
Ip√2

Fig 33

Neglecting the small amount of inductance in the turns on the coil and calling the limiting inductance L.

$$I_p \cong \frac{E_p}{L\omega}$$

Also

$$e = L\frac{di}{dt} \quad or \quad \Delta i = \frac{1}{L}\int_0^{\Delta T} e\,dt$$

Considering now the transformer action during the step. If N_p is the maximum number of turns in the AC pre-excitation winding on the core:

$$e = \frac{N_p}{N} e_c = \frac{N_p}{N} E_c\sqrt{2}\sin\omega t$$

Therefore

$$\Delta i = \frac{E_c\sqrt{2}}{L}\frac{N_p}{N}\int_0^{\Delta T}\sin\omega t = \frac{E_c\sqrt{2}\,N_p}{L\omega\,N}\left\{1 - \cos\omega\Delta T\right\}$$

Therefore

$$z = \frac{\dfrac{E_c\sqrt{2}\,N_p}{L\omega\,N}\left\{1 - \cos\omega\Delta T\right\}}{\dfrac{E_p}{L\omega}} = \frac{E_c\sqrt{2}\,N_p}{E_p\,N}\left\{1 - \cos\omega\Delta T\right\}$$

$$or \quad N_p = \frac{z\,N\,E_p}{E_c\sqrt{2}\left\{1 - \cos\omega\Delta T\right\}}$$

For this rectifier, with a Δ connection, an assumed value of z of .2, and a calculated minimum possible step length of 45.4°.

$$N_p = \frac{.2 \times 70 \times 208}{244\left\{1 - \cos 45.4°\right\}} = 33.3$$

Use 33 turns

The value of H required at the end of the break step for a β of 13 kilogauss was estimated from Figs. 17 and 18 to be .7 oersteds at 400 cycles. During the make step, there should be about .5 oersteds at the moment the contacts close to bring the flux density up to 13 kiloguass. (Fig. A-46 Appendix I.)

Assuming that the maximum value of pre-excitation occurs when a maximum value of pre-excitation is required:

$$I_p = \frac{H_2 \times L_m}{(1-z)\sqrt{2}\, N_p} = \frac{1.4 \times 19.6}{(1-.2)\sqrt{2} \times 33} = .735 \text{ amps.}$$

The value of L which will not only limit the value of z to .2 but also limit the total current to .735 amps will be:

$$L \cong \frac{E_p}{I_p\,\omega} = \frac{208}{.735 \times 800\pi} = .1128 \text{ henries}$$

The design of the pre-excitation circuits is at best approximate. Therefore, Dr. Rolf (3) recommended that the choke be made variable $\pm$ 25%. He suggested a change in the width of an airgap but multiple taps would seem to be better for this design. A total of eleven taps from - 25% to + 25% of the design value should be more than adequate.

The design of the choke was conventional using a toroidial core, silicon steel with a magnetic saturation curve shown in Fig. 34 which gave a core permeability of 4,000 at 50AT/inch. The copper winding, #22 wire (870 circular mils / ampere) with characteristcs again from Circular #31 of the US Bureau of Standards, R of 19.0 ohms/

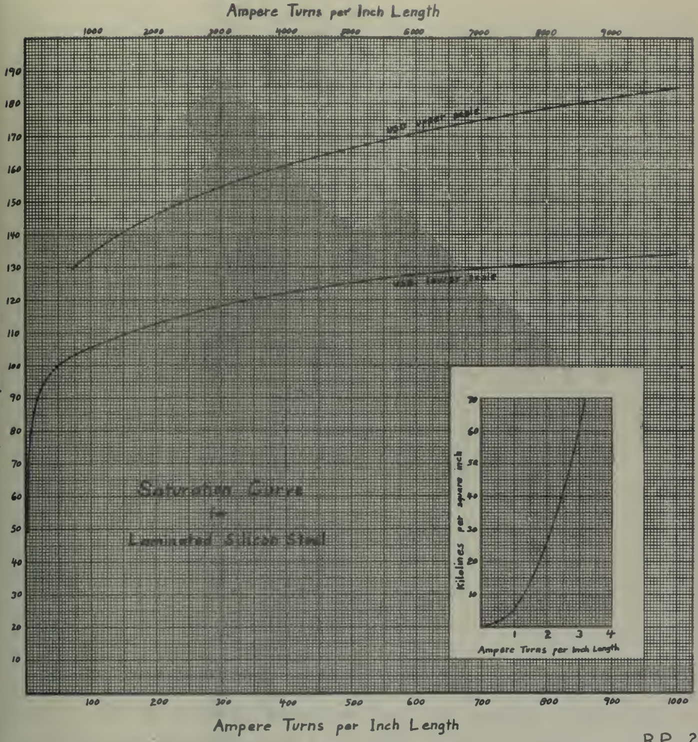


Fig 34

RP 271

11 Sept. 1940

1000 feet at 35°C and 1.94 pounds/1000 feet.

$L = .1128 \times 1.25 = .141$ h

Core area = .8 sq. cms.

$d_m = 2.9$ cms.

$N = 179$ or 36 AT/in.

Copper length on choke $= 179 \times \frac{.8 \times 4}{2.54} = 252$ in.

Copper length on main core $= \frac{18.16 \times 33}{2.54} = 236$ inches

Allowing 30 inches for internal connections

Total copper length $= 252 + 236 + 30 = 518$ inche

$I^2R/\text{core} = \frac{518}{1000 \times 12} \times 19(.735)^2 = .444$ watts

Therefore: Total loss $3 \times .444 = 1.332$ watts

Copper weight $= .8 \times 29\pi \times 7.87 \times 3 \times 2.205 \times 10^{-3}$

$= .3795$ lbs.

Allowing .2 lbs for potting

Total AC pre-excitation weight $= .25 + .3795 + .2$

$= .83$ lbs.

3. DC Pre-excitation

The DC pre-excitation has only one purpose - to limit the AC pre-excitation at loads below rated so that the output voltage will be maintained at the value dictated by the voltage regulator. This requires that the current be varied

from a minimum of zero at rated load and above, to a maximum at base load.

The design data from Chapter III showed that a maximum delay voltage area at base load was .021 volt secs. The saturation delay will account for .00656 volt seconds which leaves .01444 volt seconds to be taken care of by magnetic delay. However, the total area available is .0900 volt seconds. This means that the value of ΔB which must be available is:

$$\Delta B = \frac{\text{voltage area}}{NA_{Fe}} = \frac{.0144}{346 \times 10^{-4}} = .417 \text{ webers or } 4.17 \text{ kilogauss}$$

This means that the AC pre-excitation must be reduced so that the flux will be no greater than 14 - 4.17 or 9.83 kilogauss. This corresponds to a .2 oersteds, referring to the DC hysteresis loop, Fig. 19, to be conservative. This means that the DC pre-excitation must be able to furnish 1.4 - .4 or 1 AT/inch or a total of 1 x .9.6 or 19.6 AT.

This number of ampere turns could be supplied in almost any combination. The design of the voltage regulator would undoubtedly be the determing factor. If we assume the maximum heat dissipation to be the determing factor, and assume this maximum to be 75 watts. The limiting current would be .65 amperes at base load if we assume negligible resistance in the rest of the circuit.

The number of turns for the DC pre-excitation on each main core would then be $\frac{19.6}{.65}$ = 30.2 turns. If 30 turns is used, the next problem is a choke similar to the one designed

for the AC pre-excitation. This inductance does not have a double purpose however so that the design is not particularly critical.

If a z of .2 is again assumed, the $\Delta I_{P_{DC}}$ will be .2 x .65 or .13 amperes. Using the same design formulae as before:

$$L_{DC} = \frac{E_c \sqrt{2}\, N_{P_{DC}}}{\Delta i_p \, \omega N} \left\{ 1 - \cos \omega \Delta T \right\}$$

$$= \frac{294 \times 30}{.13 \times 800 \pi \times 70} \left\{ 1 - \cos 45.4 \right\} = .1145 \text{ henries}$$

There appears to be no reason to increase this as was done for the AC pre-excitation since it is not critical. Using #22 wire or 1000 circular mils/ampere and as before:

19.0 ohms/1000 feet at 65°C.

1.94 lbs/1000 feet

Permeability of 160 at 100 AT/inch

Core area = 2.0 sq. cms.

d_m = 3.4 cms.

N = 570 turns or 88 AT/inch

Copper length = $\frac{570 \times 2 \times 4}{2.54}$ = 1270 inches

Copper length on each main core = $\frac{18.16 \times 30}{2.54}$ = 215 in.

Allowing 60 inches for internal connections

Total copper length 1270 + 215 x 3 + 60 = 1975 inches

Total copper weight $\frac{1975 \times 1.94}{1000 \times 12}$ = .319 lbs.

Core weight $2 \times 10.7 \times 7.87 \times 2.205 \times 10^{-3} = .575$ lbs.

Allowing .2 lbs. for potting.

Total DC pre-excitation weight less regulator =

$$.319 + .575 = .894 \text{ lbs.}$$

The regulator required for this type rectifier can be any of the common types. A carbon pile would have to work opposite from the ones designed to operate in the field circuit of a generator. The minimum resistance of $\frac{115 \text{ volts}}{.65 \text{ amps}}$ or 177 ohms could either be a limiting resistor or be the lowest resistance of the regulator; varying from there to infinity.

The voltage control will be as accurate as the regulator used, until rated load is reached.

4. Base Load.

The DC pre-excitation would appear to be a perfect source of base load, and it is, except for military installations. For these, an interruption of the load at rated load or above would result in no base load until the regulator could react. For this reason, some provision must be made for a base load which will always be connected.

Any dependable load can be used but to make this design complete, a minimum base load was designed. The base load must be large enough to at least produce a flux density of 13 kilogauss. This will require a minimum of .4 oersteds. In addition to this, it must neutralize the AC pre-excitation of .7 oersteds. This means that the base load current must produce at least 1.1 oersteds, or:

$\frac{43AT}{70}$ = .615 amperes.

This is the minimum value of the base load current. The DC output voltage of course has ripple and this must be considered, either by using a choke in the base load circuit or by drawing excessive current.

The critical time exists for no external load and no DC pre-excitation, for this condition, the DC voltage would jump to approximately 135 volts since there would be no appreciable delay. This can be determined by knowing that the combination of AC pre-excitation and the base load current will produce an mmf of 1.1 + .7 or 1.8 oersteds. This gives a β of about 14.2 kilogauss or a delay of $.12 \times 10^{-4} \times 346 = .00416$ volt seconds. This will mean a voltage of $(.117 - .00416) \times 1200 = 135$ volts.

Referring again to Dr. Rolf (3), the necessary smoothing inductance is:

$$L = \frac{E_{do}}{\omega} \times \frac{\delta}{\Delta I_d}$$

For a voltage area delay of .00416, this will amount to 15° and so δ* from Dr. Rolf (3) is .21. Limiting ΔI_d by assumption to .2 amps,

$$L = \frac{135}{800} \times \frac{.21}{.2} = .0565 \text{ henries}$$

Using the same core as was used in the AC choke and the permeability of the DC choke:

N = 565 or 128 AT/inch

With #21 wire, R = 15.1 ohms/1000 feet, weight 2.45 lbs/1000 ft.

$$\text{Copper length} = \frac{565 \times .8 \times 4}{2.54} = 798 \text{ inches}$$

*This is not the same δ used by Koppelman in his work;Koppelman (2)

Allowing 15 inches for connections

Copper length = 798 + 15 = 813

Copper weight = 813 x 2.45 = .166 lbs.

Core weight = .1265

Allowing .15 lbs for potting

Base load resistor $\frac{115}{.615 + .2}$ = 141 ohms

Therefore EI = 115 x .815 = 93.7 watts

Resistor weight = .119 pounds

Therefore, total base load circuit weight is:

.166 + .2765 + .119 = .5615 lbs.

CHAPTER V

THE CONTACT MECHANISM

1. Requirements and Limitations

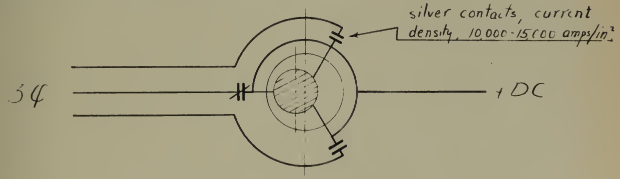
The design of the Contact Mechanism is essentially a mechanical problem. For this reason, only a brief study was made of this unit. This does not mean that there will not be some problems in making the unit work properly at 400 cycles. However, the main requirements for any contact unit, no matter what the frequency may be are:

(1) The contacts open and close at the correct time.

(2) They open faster than the voltage builds up.

(3) They seat firmly and do not bounce.

Two general systems are used for mounting the contacts, in line and radial. The radial scheme would appear to be the lightest and simpliest for this power rating. The general layout is illustrated in Fig. 35 and the push rod and contact movement in Fig. 36.



Radial Installation of Contacts

Fig. 35

The contact mechanism at conventional frequencies has posed no problem which could not be solved by good design.

The design has also been complicated by requirements for shift in contact timing, adjustment while the unit is running, and obtaining a prime mover which was stable. In this design, it was planned to have the contact mechanism driven by the same gear train which drives the alternator. This solves one of the most troublesome problems found in conventional installations, i.e. finding a small synchronous motor which is very stable and has the same angle of lag under all operating conditions. The contacts should open and close at the desired time dependably regardless of what the operating conditions may be, if the unit is geared directly to the alternator. However, the driving cam must run at a high speed. The normal method using a conventional eccentric requires a speed of 24,000 RPM at 400 cycles. The eccentric would probably have a sleeve bearing riding on it upon which would rest the contact push rods.

2. Installation and Adjustment

There must also be a provision for rotating the entire mechanism during the initial adjustment of contact timing. There are two variables involved in timing the contacts, orientation of the eccentric and overlap time. Orientation of the eccentric will affect the closing and opening time and can be changed by revolving the entire mechanism. Overlap time can be changed by changing the contact travel, Fig. 30, and so the total closed time. Juggling of these two using an oscilloscope with the voltage across the contacts on the vertical plates to show whether the contact voltage is

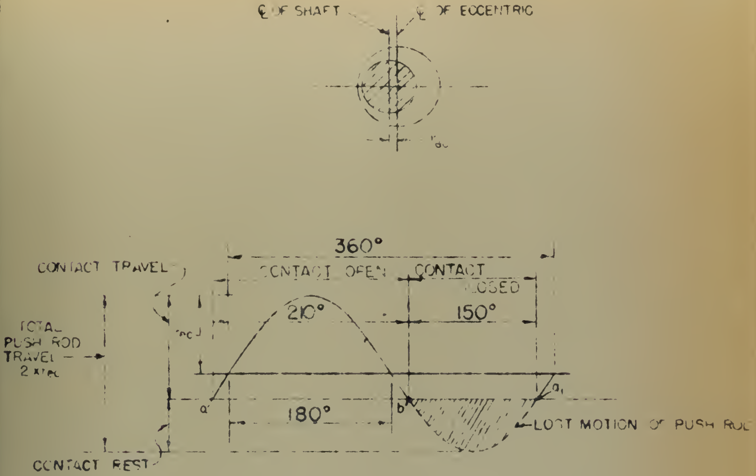


FIG 36
PUSH ROD AND CONTACT MOVEMENT

zero at closing, Fig. 2, will be required. Also, some provision for showing when, in time, the contacts should open, will be required. This can be considerably simplified, however, if the contact travel is preset during manufacture, then only the orientation need be adjusted. This is a very simple procedure using an oscilloscope. The only problem would be checking phase and phase rotation. This is common to all 3 phase power.

This may seem to be a complicated process but it need only be done at initial installation, later if the power source is changed, and for preventive maintenance, perhaps, if tests show this to be necessary.

It has been found at the conventional power frequencies that if the spring force on the individual reciprocating parts of the contact was 1000 times the weight of the part, there would be no bouncing. The speed of opening and closing need be no faster at 400 cycles than it is at 60 cycles to satisfy Koppelman's (2) requirement of an opening speed of 1 meter/second. If this is true, the accelerating forces encountered will be the same as at 60 cycles. The required eccentricity, however, for this limitation is only .4mm. While the voltages involved are low enough so that theoretically, with a pressurized space, no arc is possible, it is probable that this distance would have to be made greater to give dependable operation. During operation, there is some pitting and this tends to reduce the actual spacing to a lesser value than the contact movement would indicate. ITE uses .1 inches as a standard opening but this is dictated by

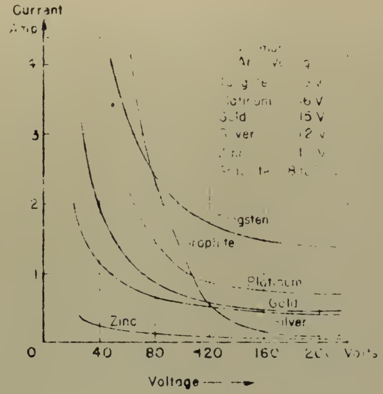
opening speed.

Dr. Rolf (3) states that there is some glow discharge if the voltage across the making contacts is more than 275 volts, for silver. In this unit, there should be zero voltage across the contacts at make. Therefore, the only criteria should be that they separate far enough so that this phenomena will not occur when the inverse voltage reaches the peak value of about 400 volts. This rectifier has so many turns on the main winding and the pre-excitation has been so designed that excessive currents at make and break should not be encountered. Fig. 37.

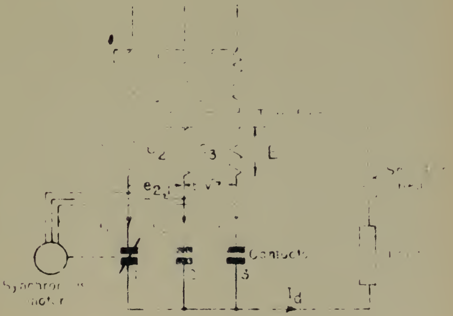
Fig. 38 shows the design used by the Germans in their small rectifiers, it would appear that a similar design would be the best system for this rectifier too. It could be easily replaced by a crewman in event of a flash over. This could be done by taking out the bolt shown and replacing the entire unit. Proper quality control would make the contacts interchangeable with no readjustment of the unit. This change then would require repressurizing in flight but nothing more.

3. The RC Parallel Circuit.

The current flowing through the contacts as they start to open has been made very small. However, to prevent contact damage by a sudden voltage build up as a result of the inductance in the circuit, the contacts are always shunted with a conventional RC circuit if the type of pre-excitation provided for in this design is used. The circuit


Current
Amp
Gold
15 V
Tungsten
Platinum
Zinc
0
40
80
120
Volts
Voltage

37


Synchronous
motor

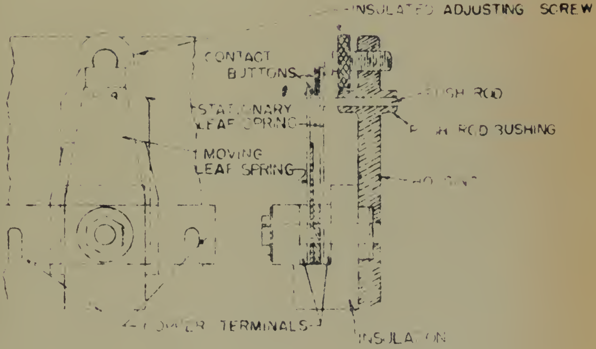
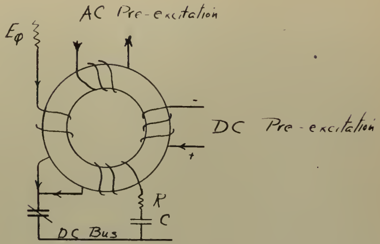


FIG 38
LEAF SPRING CONTACT

is shown in Fig. 39. The current directions are at the instant the contacts close.



Core Winding
Fig. 39

The value of R is a function of the current flowing through the contacts at the instant of opening. Dr. Rolf (3) states that the amplitude of $\Delta i \times R$ should be 10 to 15 volts.

The value of Δ i will be largest for the base load because at that time, the maximum DC pre-excitation will be aiding the AC pre-excitation. This is then the critical condition and the one which should be designed for. The actual value can be only approximated since it is only as accurate as the estimate of the probable pre-excitation. This again is a function of the size of the hysteresis loop, which changes drastically within the range of step time through which this unit will operate, .3 to .6 milliseconds. Figs. 15, 16, 17 and 18.

The maximum probable value was determined by taking the maximum AC and DC pre-excitation and subtracting the probable minimum number of ampere turns required for remag-

netization. This gives the following results:

AC	$.735 \times 33 \times \sqrt{2}$	34.3 AT
DC		19.6 AT
		53.9 AT

Figs. 15 and 16 show that the minimum field strength when the contacts open at base load will be .4 oersteds for a β of 7 kilogauss. This means that 17.7 AT will be required for actual magnetization leaving a maximum of 36.2 to produce positive current in the main winding. As a result, a maximum of $\frac{36.2}{70}$ or .512 amps will be flowing through the contacts at the time that they open. Referring again to Fig. 37, for silver contacts, there will undoubtedly be no arcing since the voltage will be low but it would be preferable if the current could be reduced more. It may be found if this design is built that the pre-excitation can be successfully reduced, this would then reduce the current when the contacts open. In practice, it has been found necessary to adjust the pre-excitation after the unit was operating.

The RC circuit is still used, even though the current is low, to reduce contact wear when this type of pre-excitation is used.

From Koppelman (2)

$$\frac{de}{dt} = 10^5 = \frac{i}{C}$$

or $C = \frac{.512}{10^5} = 5.12\,\mu f$ Use $5\mu f$.

The value of R from Dr. Rolf (3) should be:

$$R = \frac{e}{\Delta i} = \frac{15}{.512} = 29.2\,\Omega$$

Use 30 ohms.

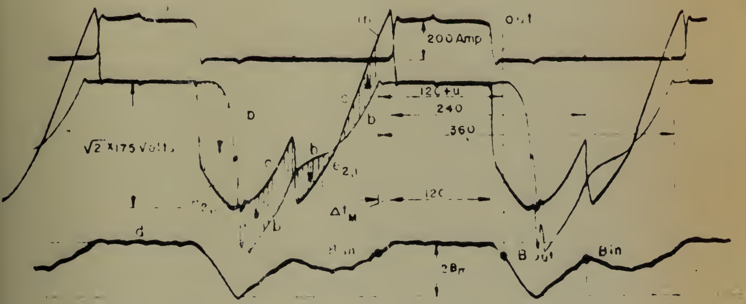
The biggest question in this particular part of the design was the wattage of this resistor. The voltage wave across the contacts is so complex, and there are so many unknowns that an exact solution does not appear practical. As an effort to obtain any kind of an answer, the wave form for the rectifier shown in Fig. 40 was used to estimate the maximum probable voltage across the contacts. From this, it was assumed that the maximum voltage across the contacts would be 400 volts. If we assume, for lack of something better, that this voltage wave can be represented by a sine wave of maximum amplitude 400 volts peak to peak and a frequency of 400 cycles, the following analysis will apply:

$$X_c = \frac{1}{2\pi f C} = \frac{1}{2\pi \times 400 \times 5 \times 10^{-6}} = 79.6\,\Omega$$

$$I = \frac{E}{Z} \quad \frac{200}{\sqrt{2}\,(30 - j\,79.6)} = 1.668 \text{ amps.}$$

$$I^2R/\phi = (1.668)^2 \times 30 = 83.4 \text{ watts}$$

$$\text{Total RC loss} = 3 \times 83.4 = 250.2 \text{ watts}$$



V1 ... volts   I1 = 2.. Amps

Fig 40

As can be seen, this is one of the major losses and it was unfortunate that it had to be based on so many approximations.

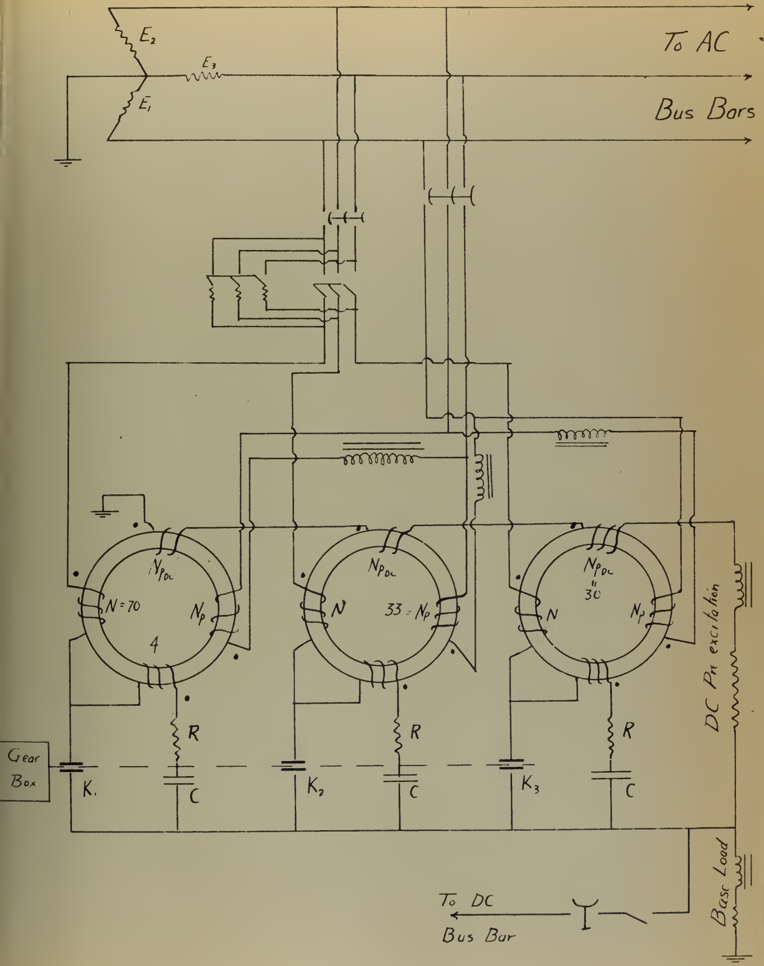
The weight of the 3 resistors and the 3 condensers in one can should be about 2 pounds.

The weight of the contact mechanism is also a guess. If an estimate is made based on a 50 ampere 60 cycle unit built by ITE, it will be a cylinder 3 inches in diameter and 4 inches long, If the weight is estimated on the basis of a piece of solid steel, 2 inches in diameter and 3 inches long, this will have a weight of 2.68 lbs. The cover for pressurizing the contacts should weigh no more than .2 pounds.

The total contact mechanism should then weigh no more than:

$$.2 + 2.68 + 2 = 488 \text{ lbs.}$$

The turns on the core of the RC circuit will supply at least the .1 Kilogauss which was not included in the saturation delay, perhaps more. This represents an area of .000346 volt secs. These turns not only aid in this way but they also prevent a large surge of current from flowing as the contacts close. From Eq (5), the number of turns should be: $\frac{.000346}{4.95 \times (1.50-1.30) \times 10^{-4}} = 3.5$. Use 4 turns.


To AC
Bus Bars
E_2
E_3
E_1
N_{pDC}
$N=70$
N_p
4
N
$33 = N_p$
30
R
C
Gear Box
K_1
K_2
K_3
DC Pre excitation
Base Load
To DC
Bus Bar

CHAPTER VI

CIRCUIT PROTECTION

It would seem from first impression that this type of rectifier would be extremely sensitive to fluctuations in frequency and voltage. However, it was shown in Chapt. III that this was not the case, as a result of the large design overload capacity.

This reduces the overload protection on the AC side to the usual thermal overload circuit breakers. There should be two sets, however, as shown in Fig. 41. One ampere breakers for the AC pre-excitation circuit and 100 ampere breakers for the main connections should be adequate.

The circuit protection on the DC side is much more complex however. This is the breaker which will largely determine whether the overload will reach too high a value. This should normally not be a severe limitation since the rectifier is largely self limiting as was shown in Fig. 25. The output voltage at the higher loads is so low that only a major fault could ever have a low enough resistance to draw that much current.

A more severe limitation is the subject of reverse current. As was explained in Chapt. III, there must always be a base load. In fact, a base load resister was put in just to supply that requirement. This means that either there must never be any reverse current, by proper adjustment of the voltage regulator, or an additional base

load must be added which will draw as much current as is required to make it react. Also, the DC breaker must always open before the AC breaker or else the unit will try to act as an inverter until the first flash over.

It would appear, that it would be much lighter and more economical to insure that a voltage regulator was used which had sufficient regulation to insure that the output of the regulator at base load was always slightly greater than any other voltage source on the bus.

CHAPTER VII

PERFORMANCE

1. Weight Estimate

The weight of each individual component was estimated as it was designed to give as accurate a composite weight as possible. The following is the weight breakdown:

			lbs.
Cores			
Iron	3 @ 4.5 lbs.		13.5
Core housing, insulation, mounting, Est.			1.0
Main winding	3 @ 1.5 lbs.		4.5
			19.0
Contacts			
Contact mechanism	Est.		5.0
RC circuit			2.0
			7.0
Pre-excitation			
AC - copper		.28	
Choke		.5	.8
DC - copper and choke		.9	
Regulator	Est.	2.5	
Base Load Circuit		.6	
			4.0
Total weight			30.8

Weight per kilowatt 3.2 pounds.

2. Volume estimate.

The cores are the largest component of any mechanical rectifier, they are usually stacked one above the other. The following is a volume estimate:

Cores - Housing 7.9 inches wide x 7.1 inches high

Contact mechanism. Est. - 5 inches long x 4 inches Dia.

Therefore the entire rectifier could be put in an enclosure 9 inches high, 9 inches wide and 15 inches long.

3. Efficiency.

	Watts
Fixed losses	
Base load resister	93.7
Core loss 3 @ 36	108.0
Contact Mechanism driving power neglected	0.0
AC pre-excitation	1.3
AC and DC choke iron loss - neglected	0.0
RC circuit 3 @ 83.4 watts	250.2
Total	453.2

Base Load

Fixed losses	453.2
DC Pre-excitation	75.0
Total	528.2

Efficiency 0%

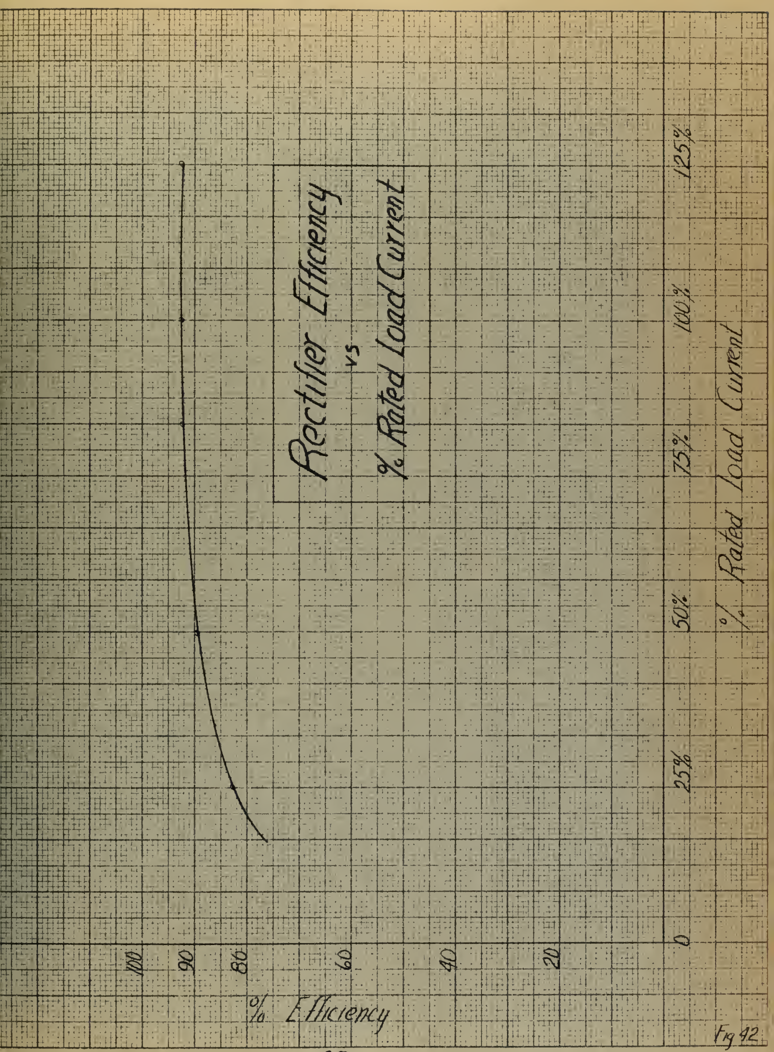
$\frac{1}{4}$ Load

Fixed losses	453.2
DC Pre-excitation	44.0
Main Winding 3 x $\frac{125}{16}$	23.4
Total	520.6

Efficiency $\frac{2500}{2500 + 520.6} = 82.7\%$

		Watts
½ Load		
Fixed Losses		453.2
DC Pre-excitation		36.0
Main winding	3 @ $\frac{125}{4}$	83.8
Total		573.0
Efficiency	$\frac{5000}{5573}$ = 89.6%	
3/4 Load		
Fixed Losses		453.2
DC Pre-excitation		22.6
Main Winding	3 @ 125 x $\frac{9}{16}$	211.0
Total		686.8
Efficiency	$\frac{7500}{8.86}$ = 91.7%	
Rated Load		
Fixed losses		453.2
Main winding	3 @ 125	375.0
Total		828.2
Efficiency	$\frac{10,000}{10,828}$ = 92.4%	
125% Rated		
Fixed Losses		453.2
Main Winding	3 @ 125 x $\frac{25}{16}$	586.0
Total		1039.2
Efficiency	$\frac{12500}{13539}$ = 92.4%	

Fig. 42 shows the Efficiency vs Load current


Rectifier Efficiency
vs
% Rated Load Current
% Efficiency
100
90
80
60
40
20
0
25%
50%
75%
100%
125%
% Rated Load Current
Fig 42

4. Power Factor

One of the disadvantages of the mechanical rectifier is its low power factor. Koppelman (2) stated that the power factor would vary from an optimum of .92 with perfect iron to a minimum of .85 with poor iron. This can be readily determined theoretically since the power factor is one minus the ratio of the reactive voltage drop to the maximum output voltage. For this unit, the base load power factor will be for 115 volts output, $1 - \frac{25.4}{140.4}$ or .82. This will improve as the load is increased and the magnetic delay is increased to a maximum at full load of $1 - \frac{20.8}{140.4}$ or .85.

The power factor could be improved slightly within the limitation imposed by the theoretical limit but only at the expense of increased weight of the core.

CHAPTER VIII

OPERATION

1. Contact timing.

The original lining up of the rectifier will require an oscilloscope as was mentioned before in Chapt. V. It is extremely important that it be carefully done since the entire operation of the rectifier depends upon the correct timing. This would have to be checked periodically as experience will dictate, as routine preventive maintenance. It is difficult to make any estimate of the maximum time between checks since no such mechanism has ever been run. However, judging on the basis of experience at 60 cycles and dividing by 7, the contacts should run at least 250 hours before any adjustment would be required.

2. Starting and Stopping.

This unit can be started very simply. The contacts will already be operating and will be synchronized by the gearing. Pre-excitation will be operating since it is presumed that this circuit will always be connected to the AC line with only circuit breakers in the line. There is no assurance that the AC voltage will be applied to the main windings in exactly the right relation to the magnetic condition of the cores existing at the last shut down. To reduce transients and avoid possible flash overs, it is necessary to first insert in the circuit, current limiting resistors which will limit the current to a maximum value of the base load plus DC pre-excitation, and reduce the

applied voltage to approximately half. These resistors need be in the circuit for only a few cycles to insure that rectifier is operating properly before rated voltage is applied.

Also, there should be some sort of interlock with the DC side, to insure that the AC switch can never be opened without first opening the DC switch. This is necessary to prevent inverter action during stopping if there are any active loads on the line. (Batteries, etc.) Also, it should not be possible to close the AC switch unless the DC switch is open. This will prevent overloading the current limiting resistors during starting and reduce the transients.

These requirements will require a three phase switch with two sets of contacts, one set of which closes about a half a second or less before the other set which will be the main contacts. This switch must also be connected by an interlock with the DC switch as outlined before.

The resistors for this circuit can be determined with the following analysis:

$$I_{Base\ Load} = .65 + .815 = 1.46 \text{ amps.}$$

$$R = \frac{E_\varphi/2}{I_{Base\ Load}} = \frac{120/2}{1.46} = 42\,\Omega$$

$$I^2R = (1.46)^2 \times 42 = 87 \text{ watts}$$

The value of I^2R need not be made as large as indicated since it will conduct for an extremely short period. If we arbitrarily reduce the value by a factor of 10, we should use a 39 ohm, 10 watt resistor.

CHAPTER IX

CONCLUSIONS

This has been an attempt to design conservatively a mechanical rectifier which could be used in large aircraft. The frequency is so much greater than anything previously used that it was necessary to design with only the fundamentals as a basis. The results obtained were in many respects, better than had been expected when the design was originally begun.

The weight for a given input and output voltage is a direct function of the line to line short circuit impedance of the power source. This means that the source must be of large capacity, the larger the better. The optimum would be a normal power line with an impedance of practically zero.

The weight could undoubtedly be reduced after one unit has been built and tested. Many parts of this design were based on an assumed optimum solution to a complex problem. The whole core problem should be investigated separately. This design considered only one value of $2W/d_m$, and only one ribbon thickness. These assumptions were undoubtedly not an optimum for both hysteresis loop size and minimum inductance. A thicker ribbon might give very little more loss and decrease the cross sectional area of the core and so lower the inductance of the coil. This would in turn, reduce the weight of the core. The minimum weight of the core was limited by the low voltage

limit of 115 volts. Experience might dictate that it was not necessary to have this much IR drop to the bus bar. Also, the line to line short circuit impedance of the source may well prove to be much smaller than the value used.

This design gave a weight of 3.20 pounds per kilowatt and an efficiency ranging from 82.7% at 1/4 load to 92.1% at rated load. The entire unit could be put in a space 9 inches x 9 inches x 15 inches.

This type of rectifier has numerous advantages over any other system envisioned. The voltage control system is very simple. The ambient air temperature will not be a problem until it reaches at least 100°C. The most critical item is the hot spot temperature of the core which should be kept below 250°C. The copper of course is completely exposed to the air. Cooling air could be taken from the supply of the alternator. ITE designs 200 ampere units with no forced cooling.

This system also has definite disadvantages; while it is not particularly sensitive to voltage and frequency fluctuations, it is mechanically sensitive during operation. This is the case because the contact timing should be kept as close to the design value as possible. The safety step will permit some leeway in the opening time, but an error in the closing time will either result in a momentary current reversal as the contact closes or a reduction in voltage at rated load. The power factor is also a disadvantage. It will never be better than .85 at any load.

This author has no weight data on other types of rectifying systems at this voltage and so no comparison can be made. However, the fact that the system is not altitude nor temperature sensitive is a tremendous advantage.

This should be a good solution for the rectification problem in general for large aircraft. It should be an excellent solution for large aircraft flying at high altitudes or where high ambient air temperature has become a problem.

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5. Papers Presented at the NOL Magnetic Materials Symposim, NOLR 1091 , 15 June 1948
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APPENDIX I

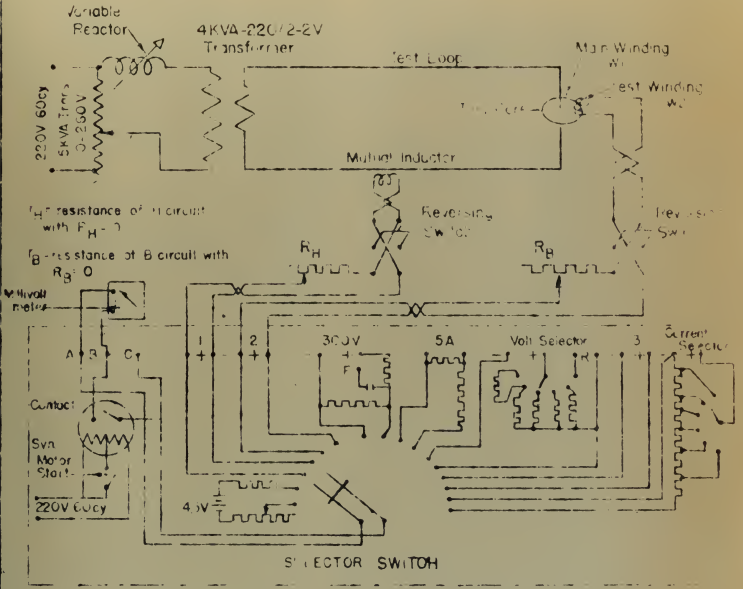
EXPERIMENTAL DETERMINATION OF THE CORE CHARACTERISTICS

The Vectormeter is the most accurate method known to obtain the dynamic hysteresis loop under conditions approximating those found in a mechanical rectifier. This loop combines the resultant of the hysteresis loop proper and the eddy current effects.

The complete circuit diagram is shown in Fig. A43. Only connections 1 and 2 of the Vectormeter were used in this particular application. The test loop shown in Fig. A 44 is 50" x 37" and can be broken to insert the test core. The loop used at NOL has a small bar which is used on the test side to accommodate the small test cores used there. The Mutual Inductor can be seen on the top of the loop and has a mutual inductance of 6.88 μ h. This is used as a pick up for measuring the value of H. The winding on the test sample consisted of 225 turns and was used to pick up the change in B. Fig. A45 is a top view of a Vectormeter being offered by ITE which closely resembles, in external appearance, the German model used in this determination.

The operation of this device is extremely simple after it has been calibrated. The voltage required to give the desired step length can be calculated by the formula given in the report:

$$E = \frac{A_{Fe}\,\Delta B \times 10^{-4}}{\sqrt{2} \times \Delta T \times 10^{-3}}$$



A E G (1947) VECTORMETER
property of
BUREAU OF SHIPS

FIG A43

TEST CIRCUIT FOR MEASURING MAGNETIZATION AND COMMUTATION CURVES OF CORE MATERIAL

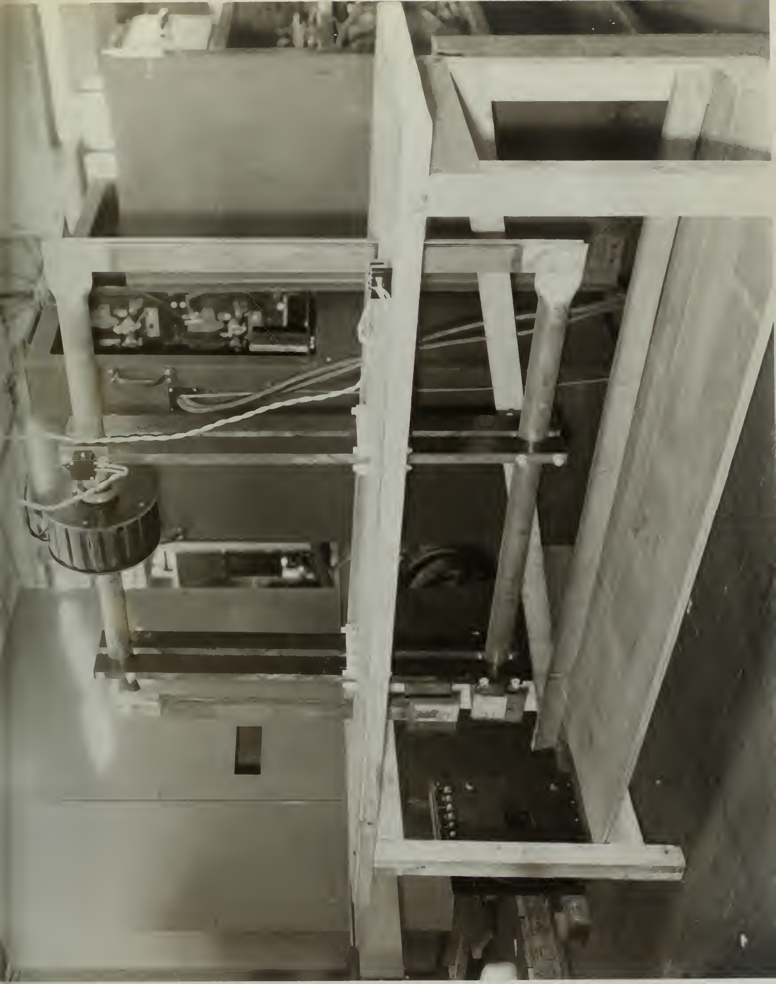


Fig A44


GND
CURRENT SELEC
CURRENT SELECTOR
CONTACT TIME
ADJ.
OFF
300V.
CONT TIME
VOL SEL
CURR SEL
VOLT SELECTOR
RUN
OFF
START

Fig A45

The calculated voltage, corrected for the transformer turns ratio, can be most conveniently read across the primary of the transformer for these small cores. This also gives a constant check on the input voltage fluctuations which were very annoying during the tests at NOL. ITE has found it necessary to use a separate supply for their Vectormeter. This problem should be very carefully considered if any new installations are contemplated since the equipment is very sensitive to even minute voltage fluctuations, in the region of the step.

The Vectormeter proper is a small single phase mechanical rectifier. The contact is gold and is set so that it is open exactly 180° and closed the same amount. The entire contact mounting can be rotated relative to the synchronous driving motor so that the opening and closing time can be varied from 0°-360°. The millivoltmeter averages the output of the rectifier and by proper calibration, will read β and H directly.

As an example, if a sine wave is applied to either terminals 1 or 2, a maximum reading will be obtained on the voltmeter when the contact is opening and closing at 0° and 180° respectively. Conversely, it will read zero when the contact is opening and closing at 90° and 270° respectively.

The following derivation should make the theory of operation clear. If further information is desired, the original article on the subject by Koppelman is Ref. (6).

For convenience, the following is a tabulation of the symbols used in this derivation and their values for this equipment.

N	No. of turns of magnetization winding	(1)
n	No. of turns of test winding	(225)
r_o	Resistance of millivoltmeter	10 ohms
R_B	Adjustable non-inductive resistor in flux test circuit (ohm)	
R_H	Ditto, current test circuit (ohm)	
r_B	Resistance of test coil and all leads in flux test circuit.	1.3 ohms
r_H	Resistance of mutual inductor coil and all leads in current test circuit	3.3 ohms
A_{fe}	Iron cross section of core under test .205 (sq.cms.)	
l_m	Mean length of core (cms) 14.46	
f	Frequency of test circuit (60) (Cycles/sec)	
e_B	Millivoltmeter reading, flux test	
e_{B_o}	Full scale millivoltmeter reading, flux test (2 mv)	
e_H	Millivoltmeter reading, current test	
e_{H_o}	Full scale millivoltmeter reading, current test (2 mv)	
E_B	Average value of voltage on test winding	
E_H	Average value of voltage in mutual inductor	
M	Mutual inductance 6.88 μh (micro henries)	
B_o	Flux density change	
B_m	Flux density	
H	2 H_o	
H_o	Magnetmotive force	

A. Calculation of R_B

The instantaneous value of e_B is:

$$e_B = nA_{r_0}\frac{dB}{dt}$$

or multiplying and dividing by f and integrating

from 0^0-180^0

$$\Delta B = \frac{f\int_0^{180^\circ} e_B\,dt}{fnA_{r_0}}$$

This operation is performed by the Vectormeter. The

average reading of the voltmeter will be:

$$E_B = e_B\,\frac{r_0 + r_B + R_B}{r_0}$$

or

$$E_B = f\int_0^{180} e_B\,dt$$

This gives:

$$\Delta B = e_B\,\frac{r_0 + r_B + R_B}{r_0 fnA}$$

or

$$R_B = \frac{fnA_{r_0}\Delta B}{e_B}\,r_0 - (r_0 + r_B)$$

To calibrate this unit for ease in reading, if we

take 20 kilogauss for maximum deflection, R_B becomes:

$$R_B = fnA_{r_0}r_0\,\frac{\Delta B_0}{e_{B_0}} - (r_0 + r_B)$$

Substituting the values for the core used and this

circuit, the final result was:

$$R_B = \frac{60 \times 225 \times .205 \times 10 \times 2 \times 2.0 \times 10^{-4}}{.2 \times 10^{-3}} - (10 + 1.3)$$

$$= 5531.3\ \Omega$$

B can now be read directly from the millivoltmeter

in kilogauss from 0 to 20 kilogauss.

B Calculation of R_H

In a similar manner to A.

$$e_H = M \frac{di}{dt}$$

Where i is the instantaneous magnetizing current

As before:

$$\Delta i = \frac{f\int_0^{180} e_H \, dt}{fM}$$

$$E_H = e_H \frac{r_o + r_H + R_H}{r_o} = f\int_0^{180} e_H \, dt$$

Considering now ampere turns per inch

$$\Delta H = \Delta i \frac{N}{l_m}$$

This makes Δ H

$$\Delta H = \frac{N e_H (r_o + r_H + R_H)}{M f l_m r_o}$$

Or

$$R_H = \frac{\Delta H f M l_m}{e_H N} r_o - (r_o + r_H)$$

Again for calibration:

$$R_H = \frac{f M l_m}{N} r_o \frac{\Delta H_o}{e_{H_o}} - (r_o + r_H)$$

This data was taken in oersteds with 2 and 20 oersteds maximum deflection on low and high scale. The resulting values were:

2 oersteds

$$R_H = 60 \times 6.88 \times 10^{-6} \times 14.96 \times 10 \times \frac{2 \times 2 \times 2.02}{.2 \times 10^{-3} \times 2.54} - (10 + 3.3)$$

$$= 81.5 \text{ ohms}$$

20 oersteds

$$R_H = 10 \times 94.8 - 13.3 = 935 \text{ ohms}$$

These calculated values set on the correct decade boxes gave the hysteresis loops used in this report. The curves taken but not used in the report proper are included in this Appendix for reference.

The data on the test core was:

NOL core 847X

ID - 1 5/8" OD 2"

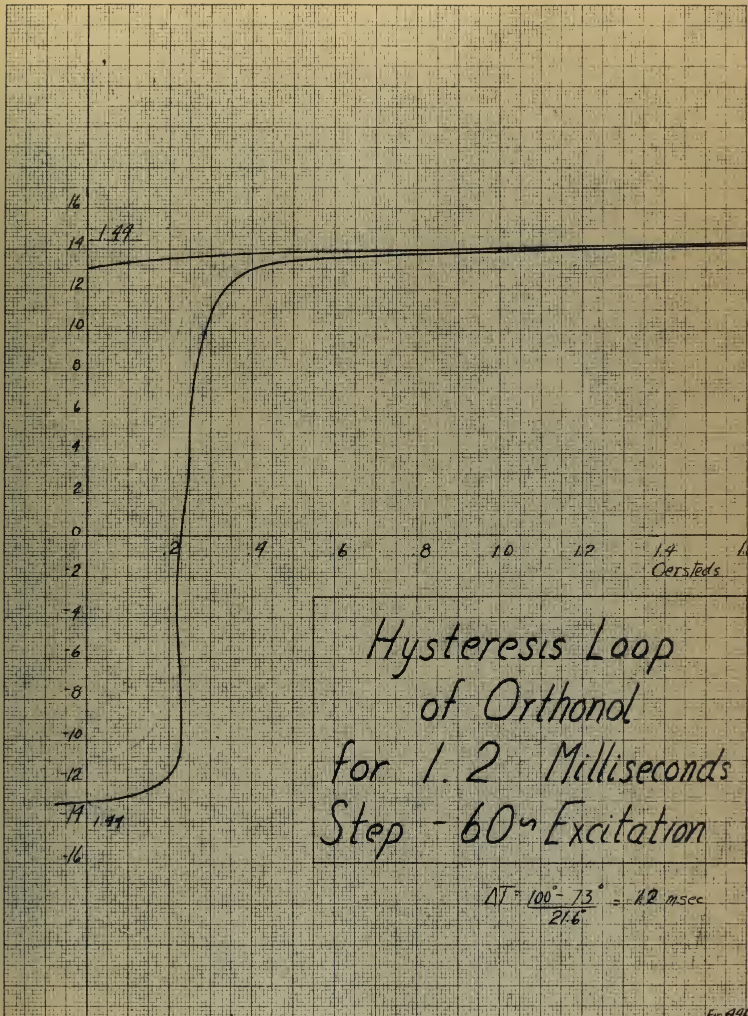
190 turns .0006" x 1/4" ribbon with magnesium oxide insulation

Weight 24.4505 gns Specific gravity 8.25, A_{Fe} - .205 cms.

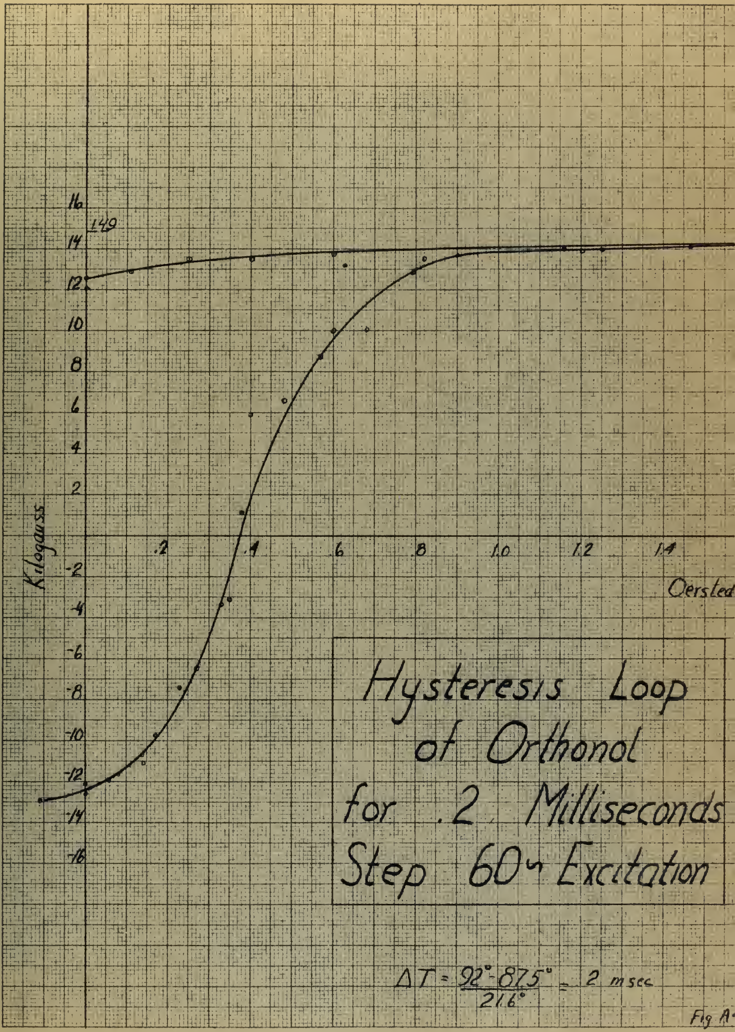
Test windings - 225 turns and 125 turns

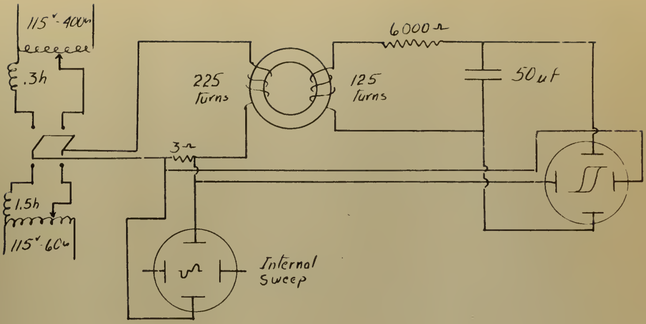
Heat treatment- 1200°C for 2 hours followed by rapid cooling. 300°C for 18 hours in 87.2 oersted field followed by rapid cooling in no field.

The hysteresis loops for 60 and 400 cycles were compared with the circuit shown below. The step length was determined by calculating the ratio of step length to a full cycle length. This was obtained by varying the input voltage until the correct length was obtained. All circuit constants were unchanged, the change from 60 to 400 cycles was accomplished with a two pole, double throw switch. The resulting hysteresis loops were taken from the surface of the tube with tracing paper.



16
14
1.44
12
10
8
6
4
2
0
.2
.4
.6
.8
1.0
1.2
1.4
Oersteds
-2
-4
-6
-8
-10
-12
-14
1.44
-16
Hysteresis Loop
of Orthonol
for 1.2 Milliseconds
Step - 60~ Excitation
$\Delta T = \frac{100° - 73°}{21.6} = 1.2$ msec


16
14
12
10
8
6
4
2
-2
-4
-6
-8
-10
-12
-14
-16
14.9
Kilogauss
.2
.4
.6
.8
1.0
1.2
1.4
Oersted
Hysteresis Loop
of Orthonol
for .2 Milliseconds
Step 60~ Excitation
$\Delta T = \frac{92° - 87.5°}{21.6°} = .2$ msec
Fig A-



Circuit for Comparing 60 and 400 cycle hysteresis loops

Fig A48

APPENDIX II

EXCERPTS FROM F. KOPPELMAN'S PAPER ON THE MECHANICAL RECTIFIER

This is an excerpt from Appendix 13 of the Dept. of Commerce publication PB 80932 as described in the Bibliography. It was included to show the derivation of the Koppelman's (2) basic formula for step length.

The German nomenclature is relatively difficult to understand without considerable study. The following list of German symbols and their English equivalents as used in the paper are included to simplify reading the Appendix.

$J = \beta = B - \mu_o H$

I_{gl_o} - $I_{\text{base load}}$

$I_{gl_{max}}$ - $I_{\text{maximum overload}}$

$I_{gl_{nenn}}$ - I rated load

t_{ein} - closing moment

t_{aus} - opening moment

ELECTROTECHNICAL PRINCIPLES OF THE MECHANICAL RECTIFIER (CONTACT CONVERTER) (Part 1)

by F. Koppelmann - Berlin

DK 621.314.5 : 621.316.543

Summary: This paper serves the purpose of enabling the reader to form an opinion of the quantitative question of the contact converter. The relation between short circuit voltage, size of choke coils, overload capacity and backfire safety is presented in Part 1. Other questions regarding the calculation of load-factor and efficiency will be dealt with in a later paper.

The contact converter is a mechanical rectifier. It was first reported before the VDE Berlin 17. September 1940. It has been built for voltage from 30 to 600 volt DC. and for loads up to several 1000 kw. Its advantage over other conversion means is its great efficiency. Its function and calculations are somewhat different from those of machine converters or mercury arc rectifiers.

A presentation of the electro-technical fundamentals of the contact converter might, therefore, be of interest. This paper treats this theme as the first of a series of contemplated discussions.

The performance of the contact converter, which has been described in a former paper, is briefly summarized here. The current interruption in the disconnected phase, which occurs at each current directive change, is accomplished in rotating machines by the commutator bar leaving the edge of the brush. In the Mercury arc rectifier, this interruption is accomplished by a medium which is non-conducting in the reverse direction. This is accomplished in the contact converter by means of a mechanical breaker with metallic pressure type contacts having openings of only a few millimeters, and driven by an excentric. The arcing of the contacts, which up to now has been a difficult problem to solve in mechanical rectifiers for large currents, has been eliminated by switching chokes having a saturable core, in the contact circuit during the working time of the particular phase. In other words during the time this phase will carry current and is saturated, but is capable on current reversal of the phase to delay the current reversal sufficiently to permit contact opening.

During this time, it is merel the magnetizing current of the switching choke that flows in the relieved phase, which is kept at a minimum by use of special high grade iron cores. The contact can, therefore, aided by a parallel condenser, interrupt this current without arcing even at high leading current, as the magnetizating current is independent of the load current, provided the opening moment of the contact occurs at the time interval during which the associated choke coil is unsaturated. The purpose of this paper is to outline the quantitative investigations, under which this condition prevails. It proves that these calculations, as well as others, which will be treated later, can be made even with sloping magnetization curves of the choke coil iron cores.

1. The Magnetization of Choke Coil During Contact Opening

We start with the magnetization of the choke coil core in the current giving off phase and at the moment that the contact is opened in this phase. If this magnetization is smaller than the saturating magnetization, then only the low dischargeless, disconnectable, magnetizing current of the choke flows through the contact at the instant of opening.

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The contact of phase 2 is closed in the moment t_{ein}, and takes over the current of phase 1 (fig. 1 and 2). At this moment, phase voltage e_2 is already greater than phase voltage e_1. Then, as with the mercury arc rectifier, the current in phase 1, is suppressed by the closing of 2, (fig. 2). With a mercury arc rectifier, the current decreases to zero, when the valve action prevents reversal of current from originating in phase 1. With the contact converter there is a somewhat similar action: The cho e coil 1, which until then was saturated in the direction of the load current, does not prevent the current to reach zero, as it practically remains saturated. The magnetization curve of the choke coil iron (fig. 3) shows that, when the current in 1 has reached a small negative value J_s' on the upper knee of the magnetization curve, a further rapid current decrease is prevented until choke 1 has reached the magnetization J_s'' on the lower magnetiz tion knee. Before that, contact 1 has to open for instance at time interval t_{aus} (fig.2.)

We will call the current flow during the remagnetization from J_s' to J_s'', that is between the time interval t_1 and t_2 (fig.2), the <u>size of the current</u>; the time interval $\Delta t = t_2 - t_1$; the <u>step length</u>; and the change of magnetization $\Delta J_s = J_s' - J_s''$ the (for the step) <u>useful magnetization change</u>. We will discuss later an exact definition of J_s' and J_s'' which is the beginning and the end of the step. It is sufficient to establish here that J_s' at the beginning, and J_s'' at the end of the step is located at the decrease of the magnetization curve, or is between the upper and lower knee of the magnetization curve. It follows from the above described short circuit of phase 1 and 2, if the ohmic resistance is disregarded and the direct current is held timely constant through the choke coil that:

$$\frac{di_1}{dt} = -\frac{di_2}{dt}$$

and for the short circuit formed via contacts 1 and 2 the voltage differential is:

$$e_2 - e_1 = L\frac{di_2}{dt} + n \cdot q\left(\frac{dJ_2}{dt} - \frac{dJ_1}{dt}\right) \qquad (1)$$

Here L is the inductance of the commutation current including air-inductance of the choke coil[2], n the no of turns and q the cross section of choke coil core. Integration between the limits tein and taus gives (fig.2):

$$\int_{t_{ein}}^{t_{aus}} (e_2 - e_1)\,dt = L\,(I_{d1} - i_{aus}) + n \cdot q\,(J_1 - J_{ein} - J_{aus}) \qquad (2)$$

Where J_1 is the magnetization of choke coil core with current I_{d1}[3] J_{ein} the magnetization of the choke coil core 2 in moment tein, also in

---

2) Under air-inductance of the core, it is understood to mean the induction the choke coil would have if it was supplied with a wooden core. Therefore the second sum on the right side of (1) is not to be calculated with the induction B, but with the magnetization I=B-H (H=field-stre gth).

3) By complete smoothing of the direct current, the magnetization J_1 of choke 1 is greater (decreasing branch of the hysteresis loop) than that of choke 2 (increasing branch). It is here understood that J_1 is the arithmetic average of the two values.

contact moment of contact 2, and Jaus the unknown magnetization of core 1 in the time moment taus of the opening of contact 1.

Formula (2) shows that the unknown magnetization Jaus is so small, (so near the negative saturation) that the greater value of the integral of the current change voltage is between tein to taus. In other words the later, at fixed t_{ein}, the break time moment t_{aus} lies, it also becomes less, with a smaller product I_{gl} . L, that is at no load, a lesser part of the steplength t of the choke coil lies behind the opening time moment t_{aus}, than at a full load (fig.4).

2. The Greatest Possible Load Variation from I_{glo} to I_{glmax} with Constant Contact Time.

The base formula of the contact converter can be directly derived from Formula 2. The equation is employed, firstly, for the maximum possible current I_{glmax}, at which the beginning t_1 of the step coincides with the t_{aus} and secondly for the loading with a lowest current I_{glo} which we call a base load. Therefore, I_{aus} is proportioned with I_{glmax} and I_{glo} if the DC current is completely saturated.

$$\int_{t_{ein}}^{t_{aus}}(e_2 - e_1)dt = L.I_{glmax} + nq(J1max - Jein + J1max - J\hat{s}) \qquad (3)$$

$$\int_{t_{ein}}^{t_{aus}}(e_2 - e_1)dt = L.I_{glo} + nq(J1o - Jein_o + J1_o - J\sigma) \qquad (4)$$

Therefore we have the magnetization Jaus, in the case of loading with base load current I_{glo} designated with $J\sigma$. For $I_{gl} = I_{glmax}$, according to the definition of I_{glmax} t_{aus} lies at the beginning of the step and is defined by magnetization $J\hat{s}$. As the left side of (3) and (4) are equal it follows that[4]

$$L.I_{glmax} + nq(J1max - Jein + J1max - J\hat{s}) = L.Iglo + nq(J1_o - Jein_o + J1o - J\sigma) \qquad (5)$$

or:

$$L(I_{glmax} - I_{glo}) = nq\left[(J\hat{s} - J\bar{s}) - (J\sigma - J\bar{s}) - 2(J1max - J1o) - (Jein_o - Jein)\right] \qquad (6)$$

and when the right side is multiplied with $\Delta Js = (J\hat{s} - J\bar{s})$

$$nq\,\Delta Js = \frac{L\,(Iglmax - Iglo)}{(1 - \sigma - \gamma - \delta)} \qquad (7)$$

by shortenings:

$$\sigma = \frac{J\sigma - J\bar{s}}{\Delta Js} \qquad (8)$$

$$\gamma = \frac{2(J1max - J1o}{\Delta Js} \qquad (9)$$

$$\delta = \frac{Jeino - Jein}{\Delta Js} \qquad (10)$$

δ is a value which normally is small, while Jein, in praxis of the usual contact converter is only altered slightly. It therefore follows that in order to simplify $\delta = 0$ if the value γ is zero, when the magnetization above $J\hat{s}$ is not increased with increased current, then we have an ideal ... for a contact converter.

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[4] This demands, that L comprises all inductive resistances up to the voltage sources in the right manner, which will be spoken of in a later work, furthermore that all voltage drops up to the voltage-sources must be neglected.

Of special importance is the value σ. From (8) $\sigma = 0$, for $J_\sigma = Js$ and $\sigma = 1$ for $J_\sigma = J\dot{s}$. σ is therefore that fraction of the total demagnetisation ΔJ_s of the core, which with base load I_{glo} lies behind the break point of the associated contact. This fraction $\sigma \Delta Js$ serves to show, at a later time, whether to make the voltage at the separating contact small or at suitable re-excitation of the choke, zero or positive. The greater σ is, the safer is the interruption against backfire. We call the fraction $\sigma \Delta Js$ at the step, the no load safety. The reciprocal fraction $(1 - \sigma)$ ΔJs of the step makes possible the load change between the limits I_{glmax} and I_{glo}. From (7) this is greater, the smaller the inductance L in the commutation current is. While at max. load, nearly the whole voltage integral of the current reversal voltage is used to change the current in the induction L. A large part of this voltage integral remains free at a small load and lays itself on the choke coil of the opening phase and depresses the magnetisation.

3. Definition of the Steplength Δt_s Referred to the Max. Value of Current Change Voltage.

Before formula (7) will be discussed, it will be transposed. On the left hand side stands a product, which characterizes the size of choke coil, namely the product of number of windings, iron cross-section and for the step useful magnetization-change ΔJ_s. If on one of J_s^+ magnetised choke coil, a constant voltage U has been laid, the result, after using the induction law, will be : $U = sq \frac{dJ}{dt}$, the time Δt, in which the magnetization under the influence of voltage U is changing from $J\dot{s}$ to Js, that is ΔJs or

$$\Delta t = \frac{\Delta Js\ sq}{U} \tag{11}$$

Step length based upon the "max. value" of the current change voltage is defined as the demagnetisation time Δt_s of the choke coil under the max. value of current change voltage. If E is the effective value of phase voltage, then at 3-phase bridge-connection

$$\Delta t_s = \frac{\Delta Js \cdot sq}{\sqrt{2}\,\sqrt{3}\,.\,E} \tag{12}$$

The calculation with this formula is easy and clear, as will be shown in the following.

4. Basic Formula for the Calculation of the Choke Coil in 3-phase Bridge Connection

With (12) we obtain from (7)

$$\Delta\ t_s = \frac{L\,(I_{glmax} - I_{glo})}{\sqrt{2}\,\sqrt{3}\ E\ (1 - \sigma - \gamma - \delta)} \tag{13}$$

We assume δ as well as I_{glo} being neglectable in (13) and call $I_{glmax} = \ddot{U}\, I_{glnenn}$, that is, we express the highest current as multiple value Ü (overload-capacity) of the rated current,

$$t_s = \frac{L\,\ddot{U}\,I_{glenn}}{\sqrt{2}\,\sqrt{3}\ E\ (1 - \sigma - \gamma)} \tag{14}$$

In this formula, the inductive short circuit voltage,[5] will be inserted

$$\varepsilon = \frac{0.5\,\omega L\,I_{nenn}}{E} \tag{15}$$

5) The formula (15) also will be reserved in this simple form for other than 3-phase bridge connections. The inductive potential drop will later replace the short circuit voltage ε, for 3-phase bridge connection $g = 0.5$. Hence: $\Delta t_s = \frac{2\,\varepsilon\,\ddot{U}}{\omega\,(1 - \sigma - \gamma)}$ (15a)

($0{,}5\ L$ = inductivity of one phase $\omega = 2\pi f$). Hereby is I_{nenn} the effective value of the secondary rectifier current. As it is known, it will be, by 3-phase bridge connection,[6] neglecting the little influence of the finite current change time

$$I_{nenn} = \sqrt{\frac{2}{3}}\ I_{glnenn} \tag{16}$$

Also

$$\mathcal{E} = \frac{\omega L\ I_{glnenn}}{\sqrt{2}\sqrt{3}\ E} \tag{17}$$

resulting out of (14) by complete smoothed DC[7]

$$\Delta\ t_s = \frac{\mathcal{E}\ .\ U}{\omega\ \ (1 - \sigma - \gamma)} \tag{18}$$

This principle formula, for the theory of contact converters, will be discussed in the following.

5. Example for the Use of the Basic Formula for the Steplength

Formula (18) is first illustrated by a practical example:

It is desired to find the (related) steplength Δt_s of choke coils for a converter, which shall, for a short time carry an overload of double the amount of current, but the time point of connection t_{ans} shall not fall outside of the step, that is $U = 2$. The defined (15) inductive short circuit voltage amounts to 7%, that is $\mathcal{E} = 0{,}07$. The frequence of DC is 50, that is $\omega = 314$. The magnetization at highest current $UI_{gl-nenn}$ is ascertained from number of turns, fom path length and magnetization curve of the choke to be:

$U\ I_{glnenn} = 1{,}6\ .\ 10^{-4}\ \frac{Vs}{cm^2}$ (16,000 Gauss), at smallest current = I_{glo} $1{,}5\ .\ 10^{-4}\ Vs/cm^2$ (15,000 Gauss)

Furthermore the usable magnetization for the step is:

$$\Delta J_s = 2{,}5\ .\ 10^{-4}\ Vs/cm^2\ (25{,}000\ \text{Gauss})$$

According to formula (9) it follows

$$\gamma = \frac{2\ (1{,}6\ .\ 10^{-4} - 1{,}5\ .\ 10^{-4})}{2{,}5\ .\ 10^{-4}} = 0{,}08$$

At no load, or with load of the basic load current I_{glo}, with consideration to the safety of contact-opening against backfire, it is required that the re-magnetization of the choke coil will take place for a half after disconnection of the relating contact. Therefore $\sigma = 0{,}5$. The result is according to (18) as follows:

$$\Delta t_s = \frac{\mathcal{E}\ U}{(1 - \sigma - \gamma)} = \frac{0{,}07\ .\ 2}{314\ (1 - 0{,}5 - 0{,}08)} = 1{,}06\ .\ 10^{-3}\,s \tag{19}$$

---

6) In each phase of the converter flow during 2/3 of the DC I_{gl} cycle, the effective value of this current is $\sqrt{\frac{2}{3}}\ I_{gl}$.

7) If the DC is completely smoothed then the approximate situation is:

$$\Delta t_s = \frac{\mathcal{E}\ \lambda\ U}{\omega\ \ (1 - \sigma - \gamma)} \tag{18b}$$

whereby λ is the proportion of the moment value of DC at beginning of current change, (time point t_{ein} in picture 2) to DC average - value.

Winding number and ion cross section of choke coils have to be chosen so that, after (12) the steplength under the max. value of the interlocked voltage will amount to about one milli-second, that is 1/10 of the part of the curve of the alternating current. By a phase-voltage $E = 140$ V.[8] and a winding number $n = 15$ we obtain for instance after (12) an ion cross section of:

$$q = \frac{\Delta t_s \sqrt{2} \sqrt{3} \; E}{\Delta J_s \, n} = \frac{1{,}06 \cdot 10^{-3} \sqrt{2} \sqrt{3} \cdot 140}{2{,}5 \cdot 10^{-4} \cdot 15} = 97 \text{ cm}^2 \quad (20)$$

For a small converter it would be chosen for instance $n = 150$ and ion cross section $q = 9{,}7$ cm^2.[9]

6. The No-load-safety

We have called that part of the step, which at no-load lies behind the point of disconnection, the no-load-safety. In the above mentioned calculation example $\sigma = 0{,}5$, that means, only the first half of the step Δt_s was used to obtain a wanted load-capacity-elbow-room between I_{glo} to $\sigma \cdot I_{glnenn}$, the second half to retard the voltage-increase on the opened contact and therewith provide safety against backfiring. If the stepcurrent opened by contact, has the value i_{ams} (pict.2), then - (considered that the magnetisation of the choke coil is a clear function of the current and not spontaneous, - i.e. no independent magnetization-changings take place) the increase of voltage immediately at contact-opening is

$$\frac{du}{dt} = \frac{i_{ams}}{C} \quad (21)$$

whereby C is the parallel to the contact lying capacity (pict. 1). For $i_{ams} = 0{,}5$ A, $C = 10 \cdot 10^{-6}$ F we obtain for instance $\frac{du}{dt} = 5 \cdot 10^{4}$ V/s.

In order to obtain a secure opening of contacts without backfire, the increase of voltage cannot overdraw a certain critical value, for instance 10^5 V/s;[10].

If under the pressure of voltage $e_2 - e_1$, according to (2), the magnetization J_s^- in the choke coil of the relieved phase is reached according to magnetization curve, increasing high current will be permitted to pass. The voltage-increase will become after (21) very much greater than the critical limit of, for instance, 10^5 V/s. If the contact opens ahead of this time, arc and backfire would occur.

We will mark this part of the step, which lies at no load in the point of time of contact opening, with (pict. 4). The time τ can be calculated assuming that, until the end of τ, the voltage on the opened contact is small, against the net voltage. Under this assumption, during

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8) Corresponding to a no-load DC voltage

$$U_{glo} = \sqrt{2} \sqrt{3} \; E = 2{,}34E = 328 \text{ V}$$

see for instance Th. Wasserrab, E. and M. 59 (1941) page 3. Calculations at voltage will be taken up in a later work.

9) In certain connections, for instance in the so far used 3-phase-bridge-connection with 3 choke coils, the steplength Δt_s cannot be very great, for instance not greater than $1{,}5 \cdot 10^{-3}$ s. The reason for this will be spoken of in a later work.

10) The conditions for arcless connections will be treated in more detail in a later work in connection with known examinations of this subject in the literature.

the time τ the whole interlocked voltage $e_2 - e_1$ lies on the choke coil of the released phase, as the second choke coil in the current-change-circuit, atthis time, is carrying the whole DC. In other words it is practically saturated. As the stepcurrent is constant, and under timely conditions changes very little, no voltages lie on the air-conductivity L of the current-turning-circuit during the τime so we obtain out of (1)

$$e_2 - e_1 = nq \frac{dJ1}{dt}$$

and by integration between the limits J and J results

$$\int_{t_{aus}}^{t_{aus}+\tau} (e_2 - e_1)\, dt = nq\,(J_\sigma - J_s^-) = nq\sigma J_s = \sqrt{2}\,\sqrt{3}E \cdot \sigma \Delta_s \qquad (22)$$

Out of this formula τ can be calculated.

If the unsaturation of the choke coil takes place from J to J_s^- under the max. value of the interlocked voltage, which is almost the case in a direct voltage to zero down-controlled converter, then we have a 3-phase connection :

$$\int_{t_{aus}}^{t_{aus}+\tau} (e_2 - e_1)\, dt = \sqrt{2}\,\sqrt{3}\; E\tau_s, \text{ that is after (22)}$$

$$\tau_s = \sigma \Delta t_s \qquad (23)$$

The index s means, that τ_s, just as well as the whole step Δt_s is based on the max. value of the interlocked voltage and represents the smallest possible value.

If the unsaturation has not taken place from J_σ to J_s^- under the max. value of the interlocked voltage, $\tau > \tau_s$, and out of (23) and (22) with $e_2 - e_1 = \sqrt{2}\,\sqrt{3}\;E \sin \omega t \psi$:

$$\cos\omega(t_{aus} + \tau) = \cos \omega t_{aus} - \omega\tau_s \qquad (24)$$

Out of this formula, any position of the disconnection-time point t_{aus} can be ascertained by the tie-up between τ and τ_s (pict.5). In converters, which are down controlled in voltage, the no load safety τ_s must be sufficiently proportioned for service with smallest DC. A greater safety is then created.

The question arises, how big practically τ_s must be chosen. The choice of τ_s is done by the following points:

1. In order to avoid backfiring by high voltage, it is not sufficient, if in the moment of contact-interruption, the voltage-increase lies for instance below the given critical value of 10^5 V/s. Rather, after contact interruption, the interlocked voltage in this moment must be smaller than the flash-over voltage of the interruption length. The flash-over voltage of the contact of known performance with level, sharp edged electrods is shown in pict. 6, also the fundamental progress over the contact-gap. Below the minimum voltage of air (about 400 V) no flash-over is possible. Even at the smallest gap, at a contact distance of 0.1 mm, that is, at a contact-interrupting speed of 1 m/s 10^{-4}s after contact separation, the flash-over-voltage is about 1,000 V, and in larger converters with usual double interrupting contacts almost 2,000 V.

   In order to avoid discharge by connection, contact converters in 3-phase-bridge connection have not been built for more than about 460 V (no-load-voltage U_{gl_0}) in one system. The largest

occurring lock-voltage which, in this connection almost equals the interlocked voltage $\sqrt{2}\ \sqrt{3}\ E$, is thereby smaller than 500 V, as in 3-phase bridge-connection $U_{glo} = \sqrt{2}\ \sqrt{3}\ \frac{3}{\pi} E$. With one value $\tau = 10^{-4}$s and a contact-interrupting-speed of 1 m/s after the timepoint - a contact-distance of 0,1 mm. and a flash-over-voltage of about 1,000 V for each interruption-distance, will be attained. That is a multiple-security against backfire.

2. If, nevertheless, contact-converters are built in praxis with values τ which are greater than 10^{-4}s, the reason is, that in practical service, there is an inexactness of contact-times, caused through thermal expansion of the gear, through damage of contact-points during service, or through tolerances during produce and these deviations from the theoretical must be reckoned with. A prolongation of contact-time, i.e. in pict. 4, a shifting of the break-time point t_{ams} to the right side, means a reduction of no-load safety. In cases of calculatively too small values and even by little differences of the contact times, there is the danger of backfire present, i.e. the converter is mechanically sensitive during service.

3. With this sensitivity to changes of contact-times and by too small no-load safety, a greater sensitivity is also given to certain electrical disturbances of the feeding DC, because, similar to a not wanted prolongation of contact-times, an increase of current-change-voltage also reduces the no-load safety. This is to be seen out of (2), where the enlarging of current-change-voltage (left side of formula) gives a reduction of Jams. By enlarging the current-change-voltage, the current i_1 (pict. 3) is decreasing faster. According to this fact, the step is beginning earlier, so that the fraction of the step which lies behind the constant break-time-point t_{ams}, is decreasing. An increase of current-change-voltage takes place not only by increase of 3-phase voltage, but partly also by voltage decrease of particular phases. During service of the converter to counter-voltage, the no-load-safety is decreasing if the counter-voltage is getting large in proportion to the produced DC voltage. The DC is then lowered beneath the base load current Iglo, and therewith the magnetisation Jlo according to (4) also the magnetisation $J\sigma$, i.e. according to (8) and the no-load-safety σ according to (23) τ_s.

For this reason a no-load safety is given the contact-converter at low voltage, which may under circumstances amount to values several times that which is necessary considering the flash-over-voltage.

In the basic formula, (18) the no-load safety τ_s is included as fraction of Δt_s, i.e. by the given σ, according to (23) τ_s becomes larger or smaller, depending whether Δt_s becomes larger or smaller. With the help of a simple transposition, the formula (18) will take a form, in which τ_s is independent from Δt_s. By inserting from (23) $\sigma = \frac{\tau_s}{\Delta t_s}$ in (18) and we obtain after solution:

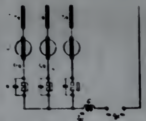
$$\Delta t_s = \frac{\varepsilon \vartheta}{\omega (1 - \gamma)} + \frac{\tau_s}{(1 - \gamma)} \tag{25}$$

This form of the formula will be used in many cases in the calculation of choke coils.

7. <u>The Value γ and the Base-load-current</u>

According to (25) the necessary steplength at a given ε, ϑ and τ_s is inversely proportional to $1 - \gamma$. The value $\gamma = 2\frac{(Jmax - Jlo)}{\Delta Js}$

1

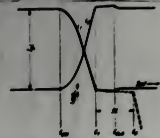


[illegible]

2 = transformer (secondary windings). 5 = choke-coil.
3 = contacts (with parallel condensators). 6 = smoothing choke.

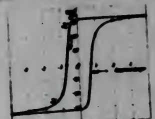
Contact converter in 3-phase-star-connection.
The picture shows the course of current-delivery from phase 1 to 2.
The current-change-proceeding is in the star-connection the same as in the practical used bridge-connection.

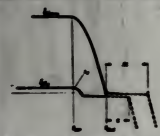
2



[illegible]

Current-change-proceeding.
The medial current-line equals the service by control of highest direct voltage by absolute sparked [illegible]. In practical service [illegible] becomes positive by pre-excitation of the choke coils.





Thesis 13133
B56 Bliss
A mechanical rectifier
for aircraft.